MASTER

Report No. ACNP-6007

**PATHFINDER ATOMIC POWER PLANT
TECHNICAL PROGRESS REPORT**

APRIL 1960 - JUNE 1960

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

October 15, 1960

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TECHNICAL PROGRESS REPORT

APRIL 1960 - JUNE 1960

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TABLE OF CONTENTS

	Page
Distribution.....	iii
List of Illustrations.....	vii
Foreword.....	xiii
Design Data.....	xv

Part A Preconstruction Research and Development

1. Fuel Element Research and Development.....	1
1.1 Fuel Material Cladding, Bonding, & Irradiation Testing.....	3
1.2 Heat Transfer and Fluid Flow Investigations.....	23
1.3 Fabrication of Fuel for the Critical Facility.....	38
1.4 Fuel Element Manufacturing Research and Development....	39
1.5 Nuclear Handling Tools.....	59
1.6 Low-Enrichment Superheater Fuel Element.....	61
2. Reactor Mechanical Studies.....	79
2.1 Vessels and Structures.....	81
2.2 Recirculation System.....	98
2.3 Steam Separators.....	101
2.4 Control Rods, Guide Tubes, & Control Rod Drives.....	108
3. Nuclear Analysis.....	115
3.1 Reactor Physics (Statics).....	117
3.2 Reactor and System Dynamics.....	135
3.3 Critical Experiments.....	145
4. Radioactive Gas Removal & Storage System & Ion Exchanger....	159
4.1 Gaseous Waste Disposal System.....	161
4.2 Reactor Water Purification System.....	162
5. Instrumentation and Control.....	167
5.1 Plant Control.....	169
5.2 Pressure Control System.....	173

Part B Postconstruction Research and Development

1. Initial Stability and Performance Tests.....	181
---	-----

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LIST OF ILLUSTRATIONS

Figure Number	Title	Page Number
I.1	Initial dynamic corrosion tests of 8001 aluminum in loops 1, 2, and 3.....	6
I.2	Reproducibility dynamic corrosion tests of 8001 aluminum alloy in loops 2 and 3.....	6
I.3	Dynamic corrosion test data for prefilmed 8001 aluminum samples.....	8
I.4	Dynamic corrosion test data for tests with increased A/V ratio.....	8
I.5	Schematic of corrosion-erosion test loop.....	13
I.6	Cutaway of irradiation test capsule 2.....	16
I.7	Section of fuel-pin sample.....	17
I.8	Fuel specimen.....	20
I.9	Typical sections of irradiated fuel pellets.....	21
I.10	Typical pellet polished and etched.....	22
I.11	Apparatus for air-water tests.....	26
I.12	Block diagram of the gamma-ray attenuation equipment.....	27
I.13	Typical data run obtained with the gamma-ray attenuation equipment.....	30
I.14	Typical set of data showing measured path lengths for an air-water mixture.....	31
I.15	Fuel tube vibration test.....	32
I.16	Water-proofed strain gage leads.....	32
I.17	Heat loss through double-walled superheater container tube as a function of stagnant steam gap.....	37
I.18	Boiler fuel element assembly.....	41

Figure Number	Title	Page Number
1.19	Nozzle-galling test assembly.....	44
1.20	Nozzle-galling test assembly.....	45
1.21	Superheater seal tester.....	58
1.22	Superheater fuel element handling tool.....	60
1.23	Uranium dioxide sintered pellet.....	66
1.24	316 L weldrawn stainless steel tubing as-received.....	71
1.25	316 L weldrawn stainless steel tubing as-received.....	71
1.26	Swaged specimen JK-1, transverse section.....	71
1.27	Swaged specimen JK-1, longitudinal section.....	71
1.28	Swaged specimen, JK-4, transverse section.....	72
1.29	Swaged specimen, JK-5, transverse section.....	72
1.30	Swaged specimen, JK-6, transverse section.....	72
1.31	Swaged specimen, JK-7, transverse section.....	72
1.32	Swaged specimen, JK-8, transverse section.....	73
1.33	Swaged specimen, JK-10, transverse section.....	73
1.34	Swage-compacted Al ₂ O ₃ rods in various stages of reduction.....	73
1.35	Swaged specimen JK-12, transverse section.....	78
2.1	Shield-pool seal arrangement.....	82
2.2	Seal ring gasket test assembly.....	82
2.3	Load-compression curves for various O-ring seals.....	83
2.4	Experimental joint design for weld between Inconel tube and carbon steel shell.....	85
2.5	Macrosections of Inconel tubes welded to a carbon steel plate with Inco. A electrodes.....	86

Figure Number	Title	Page Number
2.6	Macrosections of Inconel tubes welded to a carbon steel plate with Inco. BP-85 electrodes.....	86
2.7	Microstructures of Inco. A weld junction.....	87
2.8	Microstructures of Inco. BP-85 weld junction.....	87
2.9	Moisture de-entrainment test assembly.....	88
2.10	Steam separators.....	90
2.11	Steam sampling tube and condenser.....	91
2.12	Reactor model.....	92
2.13	Stress distribution on inside surface of spherical superheater grid plate.....	96
2.14	Stress distribution on outside surface of spherical superheater grid plate.....	97
2.15	Recirculation flow rate increase as pumps are started up.....	100
2.16	Cutaway view of steam separator test loop.....	102
2.17	Amount of air carried into steam separator with various sized air bubbles.....	103
2.18	Amount of air carried under with various sized air bubbles.....	103
2.19	Amount of air carried into separator with flow-straightening vanes in the loop.....	105
2.20	Amount of air carried under with vanes in the loop.....	105
2.21	Amount of air carried under in test loop without a steam separator.....	106
2.22	Pressure drop through steam separator.....	106
2.23	Drive housing assembly.....	109
2.24	Rack and housing assembly.....	110
2.25	Control rod drive layout.....	111

Figure Number	Title	Page Number
2.26	Operating characteristics of magnetic clutch.....	114
3.1	Relative power distribution (x-y plane) at nominal operating core condition with all control rods withdrawn.....	118
3.2	Relative power distribution (x-y plane) at nominal operating core condition with superheater control rods inserted.....	119
3.3	Relative power distribution (x-y plane) at nominal operating core condition with boiler control rods inserted.....	118
3.4	Flux peaking at gap in 81-pin region with 10 percent voids.....	121
3.5	Reactivity held by control rods and burnable poison through core lifetime.....	125
3.6	Fraction of N-16 production (atoms/sec) vs. percent reactor power.....	130
3.7	Pathfinder plant dose rates, operating floor.....	132
3.8	Pathfinder plant dose rates, mezzanine floor.....	133
3.9	Pathfinder plant dose rates, basement floor.....	134
3.10	Twenty second coastdown of all recirculation pumps due to voltage loss and self restart on 1-sec time constant.....	136
3.11	Momentary loss of one recirculation pump with P ₂ regulation....	138
3.12	Effect of void coefficient of reactivity on closed loop transfer function high-frequency transfer function.....	139
3.13	Automatic reactor power control through automatic adjustment of recirculation flow for 2-percent power increase demand with P ₂ control.....	140
3.14	Automatic reactor power control through automatic adjustment of control rods with P ₂ control.....	141
3.15	Variation of radial flux distribution after a sudden pulse of neutrons at core center.....	144
3.16	Core arrangement for boiler superheater slab experiments.....	147

Figure Number	Title	Page Number
3.17	Radial flux traverse with poisoned superheater - boiler slab geometry.....	148
3.18	Radial flux traverse, 9 x 9 boiler section, poisoned superheater.....	152
3.19	Quarter-core section showing traverse locations.....	153
3.20	Axial flux traverse, boiler section.....	154
3.21	Axial Epi-Cd traverse, boiler section.....	155
3.22	Cross-core flux traverse, 9 x 9 boiler section.....	156
3.23	Radial flux traverse, 9 x 9 boiler section.....	157
3.24	Radial flux traverse, 9 x 9 boiler section.....	158
4.1	Gaseous waste disposal system.....	162
4.2	Reactor water purification system prefilters.....	164
5.1	Reactor pressure control system.....	172

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FOREWORD

This is the eleventh 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.

Conceptual engineering and component research and development is well under way at Allis-Chalmers Greendale Laboratories. Erection of a number of major experimental facilities, which will provide important design data, has been completed. A Critical Experiment Facility was constructed and is now in operation.

*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.
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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.....	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 l in
Total core dimensions.....	6 ft x 6 ft
Dimensions of superheater region.....	6 ft x 30 in
Fuel, boiler (Al clad).....	approx. 1.8 per cent enriched UO ₂
Fuel, superheater (S.S. clad).....	approx. 93 per cent enriched UO ₂
Fuel, loading, boiler (U-235).....	117 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-ft ²
Average heat flux, superheater region.....	77,800 Btu/hr-ft ²
Maximum heat flux, boiler region.....	462,000 Btu/hr-ft ²
Maximum heat flux, superheater region.....	245,000 Btu/hr-ft ²
Recirculation rate.....	60,000 gpm
Recirculation pump power.....	794 kw
Neutron flux.....	approx. 5×10^{13} n/cm ² -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 investigations and experimental work in regard to fuel compacts, cladding materials, processes for bonding, welding and sealing of the fuel compacts leading to the development of improved and reliable fuel elements for the Pathfinder reactor.
- 2) To determine the feasibility and practicability of manufacturing boiler and superheater fuel according to conceptual design specifications.

1.1.1 CLADDING

1.1.1.1 Dynamic Corrosion Testing of 8001 Aluminum Alloy. Dynamic corrosion tests of 8001 aluminum alloy are nearly complete. A summary of test results is as follows.

Dynamic corrosion tests were initiated in Loop 1 in June 1958. Tests were initiated in Loops 2 and 3 in September and December of 1958. A total of nine dynamic corrosion tests have been completed, and a tenth, the last test of the program, will be completed in July.

Five tests were completed to establish the reference corrosion rate for aluminum alloy. The first three were conducted in Loops 1, 2, and 3 to obtain comparable results from each loop. The remaining two tests, which were conducted in Loops 2 and 3, were reproducibility runs. Because of component failures in Loop 1 after the initial test, only two loops, Loops 2 and 3, were used for the rest of the test program.

Attempts were made to reduce corrosion rates by: 1) static prefilming of corrosion specimens in an autoclave, and 2) increasing

the aluminum surface area to water volume ratio (A/V ratio) in the circulating system. Two test runs were completed in Loop 3 with two types of prefilmed samples. Two test runs were completed in Loop 2 with two variations of an excess aluminum section included in the loop; and a third reproducibility test run, the tenth and last test, as previously mentioned, is nearly complete.

Aluminum Corrosion Mechanism. The aluminum corrosion mechanism is a spall-heal process wherein a relatively thick film of aluminum corrosion product is first built up on the sample surfaces. Corrosion of aluminum then proceeds at a slower rate as corrosion film spalls and erodes from the sample surfaces. During the initial stage of corrosion, therefore, there is weight gain. During the secondary spalling-and-eroding stage, a gradual loss of weight is measurable.

Measurement of Corrosion Rate. The corrosion rate was determined by weight-loss measurements and visual inspection of specimens. In the first test run, nine samples were used in each loop, and the weight-loss reported is the average for the nine samples. In all subsequent test runs, the weight-loss is the average of eight sample coupons.

An inaccuracy in the measurements is recognized because the weight-loss measurements were made without stripping the corrosion film from sample surfaces. Instead, all samples were weighed with the corrosion film kept intact as much as possible. At the end of the test runs, however, the specimens were stripped of their corrosion film in a boric acid bath, and the total weight-loss was then accurately measured. This value served as a correction point for the corrosion rate curves.

The measurements were made at about ten-day intervals, and, within the limitations mentioned, it was possible to determine the long-term corrosion rate (the corrosion rate during the secondary steady-state corrosion stage), and the overall weight-loss.

Reference Corrosion Rate. The three initial tests and two reproducibility tests were made under the average operating conditions given in Table 1.1. The average weight-loss measured during these tests were plotted in Figures 1.1 and 1.2. The correction points appear above the last point for each curve. The initial corrosion stage lasted from 10 to 20 days. The long-term corrosion rates in the first test runs in Loops 1, 2, and 3 and the second test runs in Loops 2 and 3 correspond to rates of 23, 24, 22, 17, and 15 mils/year, respectively. The overall weight loss for the test runs, which varied from 71 to 95 days, varied from a low of about 21 mg/cm² to a maximum of about 45 mg/cm². These corrosion rates were too high to permit an economical use of 8001 aluminum alloy in Pathfinder.

TABLE 1.1
AVERAGE OPERATING CONDITIONS INITIAL 8001 ALUMINUM CORROSION TESTS

Test Number	1	1	1	2	2
Loop Number	1	2	3	2	3
Temperature (°F)	500	500	500	500	500
Pressure (psi)	1200	1200	1200	1200	1200
pH	6.2-6.9	6.3-6.7	6.6-7.1	5.8	5.9-6.4
Flow Rate (fps)	23.6	28.1	30.8	28	27
Conductivity (micromhos)	.45-.92	.2-.8	.3-.67	.4-.6	.4-.6
A/V (cm ² /liter)	45.5	45.5	45.5	45.5	45.5
Total Exposure Time (Days)	73.9	95.0	80.8	69.7	73.1

Figure 1.1 Initial dynamic corrosion tests of 8001 aluminum in Loops 1, 2, and 3. (A-C Dwg. 43-024-264)

Figure 1.2 Reproducibility dynamic corrosion tests of 8001 aluminum alloy in Loops 2 and 3. (A-C Dwg. 43-024-265)

Reduction of Corrosion Rate by Prefilming. Aluminum samples were pre-filmed in a static autoclave containing fresh distilled and demineralized water, and the samples were successively tested in Loop 3 to determine if their corrosion rates were thereby reduced. The average prefilming conditions are given in Table 1.2. The test conditions are given in Table 1.3. The corrosion test results are given in Figure 1.3.

TABLE 1.2 STATIC PREFILMING CONDITIONS

Test Number	3	4
Temperature (°F)	500	600
Pressure (psia)	681	1543
pH*	5.7	5.6
Conductivity (micromhos)*	< 5.0	< 5.0
Exposure Time (days)	7.1	3.1

*The pH and conductivity measurements were made at the end of the pre-filming period.

TABLE 1.3
AVERAGE OPERATING CONDITIONS OF TESTS WITH
PREFILMED SAMPLES IN LOOP 3

Test Number	3	4
Temperature (°F)	500	500
Pressure (psi)	1200	1200
pH	6.0	5.9-7.0
Flow Rate (fps)	27.7	26.8
Conductivity (micromhos)	.4-.8	.3-.8
A/V (cm ² /liter)	45.5	45.5
Exposure Time (days)	72.6	134.0

Figure 1.3 Dynamic corrosion test data for prefilled 8001 aluminum samples. (A-C Dwg. 43-024-266)

Figure 1.4 Dynamic corrosion test data for tests with increased A/V ratio. (A-C Dwg. 43-024-267)

The results indicate that prefilming did not reduce the long-term corrosion rate. If the correction point is interpolated by drawing through it a line parallel to the corrosion curve, about the same overall weight-loss is obtained at the end of 70 days as was obtained with untreated samples. The long-term corrosion rate for the samples prefilmed at 600F was somewhat better than the long-term rate for samples prefilmed at 500 F, but not significantly better than was obtained with untreated samples. It is possible, however, that a different prefilming temperature or method might have yielded better results. Nonetheless, due to the high flow conditions and frequent cycling, the prefilm coating may be expected to wear or spall rather rapidly, and its usefulness does not seem promising.

Reduction of Corrosion Rate by Including Excess Aluminum. An attempt was made to reduce corrosion rate by increasing the A/V ratio in the circulating system. The test conditions during three successive tests are given in Table 1.4.

TABLE 1.4
AVERAGE OPERATING CONDITIONS INCREASED A/V TESTS IN LOOP 2

Test Number	3	4	5
Temperature (°F)	500	500	500
Pressure (psi)	1200	1200	1200
pH	5.9-6.3	5.6-6.3	5.3-6.0
Flow Rate (fps)	27.6	28	27
Conductivity (micromhos)	5.0	.35-.60	.4-.6
A/V (cm/liter)	2620	134.6	134.6
Total Exposure Time (days)	57.0	107.0	103.9

In the first of these tests, an aluminum rack was placed in the by-pass stream, which was previously used to purify the circulating water.

In the second and third of these tests, an excess aluminum section was located in the main-loop piping directly upstream from the samples, and the by-pass purification system was placed back in operation. Test results of the three tests are shown in Figure 1.4 and indicate that increasing the A/V ratio reduced the corrosion rate provided the water is continuously purified and provided the excess aluminum is located directly upstream from the sample.

By increasing the A/V ratio, the corrosion product concentration in the circulating water is increased. The excess area contributes to an increase in the concentration of solids in the circulating water; whereas, the purification stream reduces the solids content. These two conditions which are at odds, both appear to be necessary in reducing corrosion rate, and no sound reason can be given for this at this time.

Conclusion. The dynamic corrosion testing program of 8001 aluminum alloy was carried out as planned. The pace of the program was governed by the length of time required to arrive at a steady-state corrosion rate in each test. During the tests some delays were caused by various equipment malfunctions.

The corrosion rates obtained at Greendale compare well with results from other laboratories. Although corrosion rate was significantly reduced by increasing the A/V ratio in the last two test runs, much research is necessary to fully explain the reasons for the reduced corrosion rates. Further experimentation is also necessary to determine

the effect of pH, the effect of various inhibitors, and the characteristics of aluminum under conditions of heat transfer.

It is the concensus of investigators at Allis-Chalmers, as well as at other laboratories, that the present state of knowledge about dynamic corrosion characteristics of the 8001 alloy is not advanced sufficiently to permit it's use in a boiling-water power reactor such as the Pathfinder. The corrosion program will therefore be discontinued in July. This project will no longer be reported in this quarterly progress report, but, a final report of the program will be prepared and published.

1.1.1.2. Corrosion-Erosion Test of Stainless Steels. In the Pathfinder reactor, saturated steam from the boiler region passes down through a central superheater section containing fuel tubes clad with stainless steel. The surface of the stainless steel fuel-tube cladding will acquire the usual protective oxide film in a rather short period of time. This film will become increasingly radioactive with time. Because of the radioactive nature of this film, and because the Pathfinder is a direct cycle plant, it is important to determine the amount of this oxide film that will be carried over with the steam.

A test will be conducted to determine the corrosion-erosion characteristics of austenitic stainless steel in a steam atmosphere.

More specifically, the test should enable a determination of the following:

1) The effect of up to 1/2 per cent water in steam at 600 psi and 489 F on corrosion-erosion and film release rates of stainless steel.

2) The effect of up to 100 ppm free oxygen in saturated and super-saturated steam at 600 psi and 489 F on corrosion-erosion and film release rates of stainless steel.

3) The effect of steam velocity on corrosion-erosion of stainless steel.

4) The effect of surface pretreatments on corrosion-erosion rates.

5) The type of stainless steel most suitable for use in the Pathfinder superheater.

Since austenitic stainless steel has unusually good corrosion resistance in steam atmospheres at moderate temperatures, the conventional methods of determining corrosion rates by specimen stripping and weight-change measurements may not be adequate. Consequently, use will also be made of one neutron activated specimen.

Test Loop Design. The corrosion-erosion test assembly consists mainly of two concentric steel pipes. The outer pipe, the housing assembly, is a 12-in diameter schedule-80 carbon-steel pipe. The inner pipe, the test section pipe, is a 2-in o.d., 1-1/8-in i.d., type-304 stainless-steel pipe. Test specimens will be located in the test-section pipe at three stations. Design is such that each of the

Figure 1.5 Schematic of corrosion-erosion test loop. (A-C Dwg. 43-202-043)

three stations can be points of different moisture concentration. The upper station will contain a nest of three concentric tubes of different stainless steels. The middle and lower stations will contain identical single stainless steel tubes except that the lower tube will have been irradiated.

A flowmeter and flow control valve are located downstream of the test section pipe a short distance from where it emerges from the housing assembly. A detailed sketch of the loop is shown in Figure 1.5. Just after the flow control valve a pressure vessel that holds steam filters is located. Within the pressure vessel, two absolute filters are placed in series. Backing up the filters is a particle trap that has an efficiency of better than 99 per cent for removal of particles 10 microns in diameter or larger. From the particle trap, the steam passes through a throttling valve to the plant blow-off system from which it is discharged to the atmosphere.

Test Operation. Powerhouse steam reduced to 600 psi is introduced into the bottom of the housing assembly. It passes through a column of water and causes saturated steam to boil from the surface of the water. This saturated steam at 600 psi, 489 F, then passes through a demister, makes a 180-degree turn, and enters the test section pipe. In the test section pipe the steam is accelerated to the desired high velocity and is passed over the surfaces of the test specimen. Here corrosion-erosion products are entrained in the steam and are carried through the flowmeter orifice and control valve, and are captured in the filters. The steam exhausts to the atmosphere

through a particle trap. Weight-change measurements will be used to determine corrosion-erosion rate of the non-radioactive specimens; the filters will be checked for radioactivity to determine the carry over of radioactive material.

The evaluation of the corrosion-erosion products collected on the filters will consist of first melting down the fibre-glass filters and then making a count of them. The counting data will be normalized to determine the weight of corrosion-erosion product collected. An attempt will be made to utilize gamma-ray spectrometry to determine the isotopic composition of the corrosion-erosion products. In this manner, the nature of the test specimen surface film may be determined.

1.1.2 IRRADIATION STUDIES

1.1.2.1 Capsule Irradiation Tests. The purpose of the program was to irradiate aluminum-clad fuel pins to a burnup of 5,000, 10,000, 15,000, and 20,000 MWD/T to determine irradiation effects on fuel and cladding and the adequacy of the design. The fuel-pin specimens were contained in four capsules for irradiation in the GETR. The criteria for the capsule design was 500,000 BTU/hr-ft² maximum heat flux and 500 F at the surface of the aluminum cladding. Heaters were placed in the capsules to maintain the cladding temperature at 500 F when necessary. Thermocouples were installed to check and record the temperatures at the surface of the capsule and the fuel specimen cladding. This provided a means of calculating the heat flux pro-

Figure 1.6 Cutaway of irradiation test capsule 2
(Courtesy of Vallecitos Atomic Laboratory)

duced by each fuel specimen. A drawing of the capsule is shown in Figure 1.6.

Three fuel-pin specimens were included in each capsule. Each fuel-pin specimen contained fuel pellets 0.338 in. dia. and approximately 0.580 in. long. The pellets in Capsules 1 and 3 have a flat face on each end. The pellets in Capsules 2 and 4 have small dished faces on each end. Each fuel-pin specimen consists of four pellets plus a fission-gas space and is clad with 8001 aluminum alloy. The aluminum was swaged to provide intimate contact of the fuel and cladding. The U-235 enrichment for the three fuel specimens per capsule was 4.5, 6, and 7 per cent. Because of the change in neutron flux along the length of the capsule, it was necessary to vary the enrichment of the fuel specimens to maintain the same heat flux from each specimen. A detailed drawing of the fuel specimen is shown in Figure 1.7.

Figure 1.7 Section of fuel-pin sample. (A-C Dwg. 43-024-217)

The pre-irradiation examination consisted of:

- 1) Weight and density measurements of the individual fuel specimens.
- 2) Measurement of the outside diameter and length of the individual fuel specimens.
- 3) Photographs of individual fuel specimens.
- 4) Measurement of capsule internal gas pressure. (Capsules were pressurized to obtain calculated 600 psi under irradiation.)
- 5) X-ray photograph of the assembled capsule.

Capsules 1, 2, and 4 were inserted in GETR for the October cycle. Due to early failure of heaters and thermocouples in all capsules and due to unexpectedly low temperature readings at the end of the reactor cycle, as indicated by the remaining thermocouples, it was decided to remove one capsule from the reactor at the end of the December cycle. Since Capsule 4 had only 1 of 7 thermocouples and no heaters functioning, it was removed, and an immediate post-irradiation examination was made.

The fuel specimen positions and enrichment within Capsule 4 are given in Table 1.5.

Capsule 4 was irradiated for three cycles in the GETR. Reactor data for these three cycles and the temperatures recorded are given in Table 1.6.

Table 1.5 CHARACTERISTICS OF FUEL-PIN SPECIMENS IN CAPSULE 4

SPECIMEN NO.	POSITION	ENRICHMENT (per. cent)
A-1-1	Bottom	4.49
B-1-3	Middle	5.96
C-1-4	Top (TC end)	6.92

Table 1.6 REACTOR & SAMPLE DATA DURING IRRADIATION OF CAPSULE 4

CYCLE NO.	START	END	TOTAL POWER (THERMAL) (MWD)	TEMPERATURE		
				MAX.	MIN. (F)	AVE.
6	10-10-59	11-3-59	437	---	320	---
7	11-8-59	11-30-59	668	400	310	360
8	12-11-59	12-28-59	538	385	290	335
		*	1633**			

* Total time in reactor 79 days.
 **Total time of reactor power was 54.5 days.

Examination of Capsule 4 did not reveal the cause of the heater and thermocouple failures. When the capsule was disassembled and the fuel specimens were removed, no radioactivity was present in the helium or NaK, thus indicating that no fission gas or fission products had leaked from the fuel specimens.

A visual examination of the cladding of the fuel specimens did not reveal any significant change when compared to the pre-irradiation condition. There was no evidence of deformation, bowing, or discoloration. Each fuel specimen was checked for changes in dimensions, weight, and density. The results showed that, within experimental error, no significant changes had taken place during irradiation.

A sample of the released fission gas from each fuel specimen was analyzed for krypton-85. This data along with the burnup analysis permitted the calculation of fission-gas data, which is given in Table 1.7.

Table 1.7 FISSION GAS DATA

SPECIMEN	TOTAL ATOMS Kr ⁸⁵ RELEASED	TOTAL ATOMS Kr ⁸⁵ GENERATED	FISSION GAS RELEASED (PER CENT)	CALCULATED P (PSI) AT 0°C
A-1-1	2.99×10^{15}	8.35×10^{17}	0.36	0.27
B-1-3	2.27×10^{15}	8.20×10^{17}	0.28	0.20
C-1-4	1.52×10^{14}	6.27×10^{17}	0.02	0.01

Table 1.8 BURNUP DATA

SPECIMEN	ATOMS U-235 FISSIONED PER MILLIGRAM U	AVERAGE FLUX AT 30 MW (nv)	AVERAGE HEAT FLUX (BTU/hr-ft ²)	U-235 ATOM FRACTION BURNUP	MWD* TON U
A-1-1	9.41×10^{15}	3.74×10^{13}	370,000	.094	3240
B-1-3	8.97×10^{15}	2.72×10^{13}	364,000	.0695	3180
C-1-4	6.89×10^{15}	1.78×10^{13}	283,000	.046	2480

* Megawatt Days/Metric ton of total uranium.

Each fuel specimen was sectioned according to Figure 1.8. The cut face showed longitudinal, radial, and transverse cracking of the UO₂ pellets. Typical sections can be seen in Figure 1.9. After cutting, sections B-B, C-C, and D-D were prepared for metallographic examination. Each sample was carefully examined for evidence of

Figure 1.8 Fuel Specimen
(A-C Dwg. 43-024-217)

center melting and/or grain growth. No evidence of either phenomena was found (Figure 1.10) indicating that the center of the UO₂ pellets probably did not reach the original sintering temperature of 1700 C.

A section of fuel, which is designated by "X" in Figure 1.8, in each fuel-pin specimen, was dissolved and radiochemically analyzed for cerium-144 to determine the burnup of U-235. Based on the cerium-144 determination, the data given in Table 1.8 were calculated.

The results of the post-irradiation examination showed that the capsules were not receiving the necessary thermal neutron flux to provide the desired heat flux and surface temperature and that the effective thermal conductivity of UO_2 was greater than the $0.85\text{-Btu/hr-ft}^2\text{-}^\circ\text{F/ft}$ value used in the design calculations. The cause of

Surface D-D Specimen B-1-3

Surface B-B Specimen C-1-4

Figure 1.9 Typical sections of irradiated UO_2 fuel pellets.
(Courtesy of Vallecitos Atomic Laboratory)

Figure 1.10 Typical pellet polished and etched showed no center melting and/or grain growth. Magn: 250 X (Courtesy of Vallecitos Atomic Laboratory)

low neutron flux was partially due to a control rod shadow during one of the cycles and partially due to operation of GETR on shorter cycles than originally planned.

As a result of the capsule-4 examination, the irradiation program for the remaining three capsules was revised in the following manner:

- 1) Capsule 2 remained in its original position. It was removed from the reactor on May 16 after a burnup of approximately 10,000 MWD/T.

- 2) Capsule 1 was lowered 4-1/2 in. in the reactor from the original position. Calculations indicated that a maximum heat flux of 500,000 BTU/hr-ft² and a maximum surface temperature of 500 F would be achieved in this position. It will be removed at the end of August and will have a burnup of approximately 15,000 MWD/T.

- 3) Capsule 3, which was placed in GETR during the December shutdown, was lowered seventeen inches to place it in a higher flux region. It is anticipated that this position will produce center melting and/or grain growth in the UO₂ pellets. It was removed from the reactor on June 19 with a burnup of approximately 10,000 MWD/T.

- 4) Capsules 2 and 3 will be examined during the month of July.

- 5) Capsule 1 will be examined during the month of September.

1.1.2.2 Irradiation Study of Superheater Fuel. A cermet of highly enriched UO_2 and stainless steel is reference fuel for the first superheater loading of Pathfinder, and a detailed study of available data on its irradiation stability is being made. This data will be used to establish the design parameters for the high enrichment fuel elements. Additional information will be obtained through post-construction irradiation tests in Pathfinder.

During the period, irradiation damage data for Supplement 4 to the Preliminary Safeguards Report for Pathfinder was revised. No further work on this study is planned until a decision by the Hazards Evaluation Branch is given on this material.

1.2 HEAT TRANSFER AND FLUID FLOW

The objectives of this project are as follows: 1) to experimentally determine the actual heat transfer and fluid flow characteristics of the boiler fuel elements so as to establish and improve performance under conditions of high velocity, two-phase flow; and 2) to experimentally determine the actual heat transfer and fluid flow characteristics of the high enrichment superheater fuel elements.

1.2.1 HEAT TRANSFER LOOP

No tests were made during the quarter because the Unipolar generators were not operating properly. In late May it was decided to investigate the possibility of obtaining data from the one-pin, 10-8-6-pin, and high-enrichment superheater test section either by

using another power supply or by locating another laboratory that could conduct the tests. A motor-generator unit was found available in Boulder City, Nevada which is capable of providing the necessary power. Argonne National Laboratory can test the 10-8-6-pin test section and possibly the one-pin section but cannot test the superheater test section. The motor-generator unit was purchased because not only could all testing be done but valuable experience could be gained as well.

The power supply is a 3-unit M-G set consisting of a 700-hp, 3 phase, 60 cycle, 2300 volt synchronous motor and two independently controlled 225-kw, 5000-amp, 45-v, d-c generators. The excitation current for this set is supplied by an 8-bearing unit consisting of a 30-hp motor, a 7-1/2 kw d-c generator, and two 5-kw, d-c generators. The output voltage of the 225-kw generators can be varied from 0 to 45 v by changing the excitation current with a hand rheostat.

Plans for the motor-generator's base and shelter were drafted and construction was started. The unit was dismantled and shipped to the West Allis Works for minor maintenance. The Greendale Laboratory transformer was modified to accommodate the 700-hp motor.

1.2.2 BOILER FUEL ELEMENT

1.2.2.1 10-8-6-Pin Test Section. A cold-water, single-phase pressure-drop test was run with the 10-8-6-pin test section. The water flow rates varied from 40 to 160 gpm corresponding to reactor boiler

flow rates of 209 to 835 gpm. The test was run twice to determine reproducibility. The data from the two tests agreed to within 2 per cent. Calculations were made on the 81-64-49-pin boiler element to determine the single phase pressure drop under the test conditions. The friction factor was based on smooth tubing. The transition losses were based on an expansion plus a contraction loss where the area ratio was the box area minus the area of the rods divided by the box area. A general comparison between the calculated and test results is as follows:

- 1) Friction drop - calculated 10 per cent low
- 2) Transition loss - calculated 2-1/2 times low
- 3) Overall loss - calculated 10 to 15 per cent low.

The overall test pressure drop for the test flow ranges was 1.5 to 16.2 psi.

1.2.2.2 81-64-Pin Test Section. The design of an 81-64-pin full-sized test section was completed, a layout made, and all the raw materials selected.

1.2.2.3 Low-Pressure Air-Water Test. Figure 1.11 shows the experimental apparatus being used for the low-pressure air-water tests. The large manometer panel extending from the left foreground to the center of the picture is used to measure the various pressure-drop

Figure 1.11 Experimental Apparatus for the Air-Water Tests
(A-C Photo 207756)

Figure 1.12 Block diagram of the gamma-ray attenuation equipment. (A-C Dwg. 43-202-005)

components across the test sections. Six additional manometers are located to the right of the manometer panel.

The gamma-ray attenuation method being used to determine voids is based on measurements in the variation of gamma-ray attenuation as water and air in various ratios flow through the test section. Only gamma-rays between 78 KEV and 90 KEV are counted. Figure 1.12 shows the components used in measuring the void fraction. Two radioactive sources, Tm-170 and Cs-137 are used. The Tm-170 is used for the low-pressure air-water studies, and the Cs-137 is used for the measurements in the heat transfer loop. The radiation source is in a lead cylinder located on the positioning rack. The rack shown in Figure 1.11 is remotely controlled to move both vertically and horizontally. The scintillation detector and pre-amplifier share a common housing on the positioning rack. The test section is placed between the detector and source for operation. The amplifier, count-rate meter, pulse-height analyzer, and a printer are also shown in Figure 1.11. The chart recorder, which is not visible, is located to the right of the equipment shown on the table.

The water and air flows into the test section from below. This section is behind the manometers. Water flow is measured using a sharp-edged orifice calibrated against a water meter and the Lammar flow element. The air flow is measured with a Meriam Instrument Co. 50 MA15 "Lammar" flow element. Air leaves the system at a free surface above the top of that which is pictured, and the water flows through the large pipe seen in the upper left hand corner of the picture back to an open receiver.

During the quarter, test runs were made to obtain reliable void fraction data with the equipment described. The test section used is a partial mockup of the boiler fuel element, and is made of 8001 aluminum alloy. It consists of a thirty-six-pin 4×9 array four feet long as shown in Figure 1.12. Pressure taps are located 6 in. apart along its length. All void measurements were made about 2 in. upstream of a set of spacers that hold the fuel pin array in place. The test section fuel pins are hollow and welded shut on either end so that only air is inside the fuel pin. The hollow pins permit more accurate void measurements when shooting through a row of fuel pins.

A sample set of data (Figure 1.13) shows three traverses of the test section: 1) full of water, 2) full of air, and 3) full of an air-water mixture. The fuel pin cross sections are superimposed on the data as reference for interpreting the data.

In a traverse of the test section, seventy-nine timed shots of forty-second duration are made. These shots are spaced from 0.020 to 0.040 in. apart depending on the location of the shot. The width of the collimated gamma-ray beam is 0.060 in. The attenuation of the gamma-ray beam is assumed to be exponential, so that the natural logarithm of the ratio between the air and the water filled traverses give the water path length, as shown in Figure 1.14. If one determines the area under the curve and divides by the average experimental absorption coefficient, μ , the cross-sectional flow area is obtained. The cross-sectional flow area measured by this method

Figure 1.13 Typical data run obtained with the gamma-ray attenuation equipment. (A-C Dwg. 43-202-007)

Figure 1.14 Typical set of data showing measured path lengths for an air-water mixture. (A-C Dwg. 43-202-003)

Figure 1.15 Fuel tube vibration test. (A-C Photo 207758)

Figure 1.16 Water-proofed strain-gage leads. (M-P Photo 8A-0-8)

is five per cent less than the area calculated from physical measurements. Most of this difference is believed to be due to non-exponential attenuation of gamma rays that pass through a side of a row of pins.

The air path length of the mixture is found in the same way and is also plotted in Figure 1.14. The void fraction is merely the ratio between corresponding values for the two curves.

1.2.2.4 Fuel Rod Vibration Test. The vibration test outlined in the last quarterly report was completed during the quarter. The test was performed in a closed loop as shown in Figure 1.15, with provision for air injection into the water for two-phase flow. The test section consisted of two Lucite tubes gasketed into heavy end brackets, which also support the test section. The test section was designed to reproduce the geometrical properties and end-fixity that will be used in Pathfinder. The rod vibrations were picked-up by four foil strain gages and were recorded by standard recording oscillographs.

A novel technique was used for making and water-proofing the strain-gage leads. The leads were 1/8-inch wide strips of 0.001 in. thick copper. A layer of water-proofing agent was brushed on the test rod, the strain gages and leads were joined together and glued on, and the whole assembly was given several coats of water-proofing. The technique, which is illustrated in Figure 1.16, worked very well.

The test had three objectives:

1) to duplicate the single-phase work of D. Burgreen, et. al.*

2) to determine the effect of two-phase flow on vibration amplitudes.

3) to correlate vibration amplitude with void fractions, water velocities, and hydraulic diameter, if two-phase flow had the expected marked effect on amplitude.

As a result of these tests, the following conclusions can be stated:

1) Vibrations due to water flow only were nil. For example, the 36-in rod had an amplitude of about 0.002 in. at a velocity of 21 fps, which is much less than the 0.010 in. predicted by Burgreen's analysis. This result was well substantiated throughout the test program.

2) The natural vibration frequency of the rods was not reduced by the surrounding water. The frequency recorded when the bare rod was struck by a hammer was essentially the same as that recorded while the rod was standing in still or flowing water.

* ASME Paper No. 57-A-94, "Vibration of Rods Induced by Water in Parallel Flow," D. Burgreen, et. al.

3) The addition of air to the water flow changed the vibration amplitude so significantly that the single-phase analysis for vibrating rods is not applicable. Analytical predictions of the vibrations that rods will under-go in two-phase flow is further hampered by the fact that there are numerous flow regimes, which affect the vibrations depending on the regime present. Some of these regimes are sluggish annular, slug, stratified, plug, and annular froth. With a slug-type flow, the vibrations can become excessive. However the slug regime occurs at flow velocities and void fractions much greater than those present in Pathfinder.

4) Vibration amplitudes of about 0.006 in. were attained by the test rod that most nearly simulated the actual Pathfinder fuel rod section, at Pathfinder conditions, i.e. 25-fps flow, 60 per cent void.

The comparisons between the test rod and the actual rod are given in table 1.9.

Table 1.9 CHARACTERISTICS OF PATHFINDER FUEL ROD & TEST ROD.

	Pathfinder	Test Rod
length, in.	18.39	18.41
weight per in., lb/in	0.041	0.041
EI, lb-in ²	8000	11,300
natural frequency, cps	40.6	47
hydraulic diameter, in.	0.620	0.600

Since the rods compare so closely and the test rod vibrations were not excessive, the Pathfinder rod is expected to perform satisfactorily. Two-phase air-water flow tests of the boiler element prototype, which is now being fabricated, will be made to substantiate this conclusion.

1.2.3 SUPERHEATER FUEL ELEMENTS

1.2.3.1 Heat Transfer Loop Test Sections. The high-enrichment superheater test section for the heat transfer loop was assembled.

The low-enrichment superheater test section was conceptually designed and a layout was made.

1.2.3.2 Air-Flow Test of 7-Pin Scalloped Flow Tube. Air flow tests of the 7-pin septafoil that is being investigated as a low-enrichment superheater fuel element design show a pressure drop 25 per cent higher than calculated. This was previously attributed to rotational acceleration imparted by the spiral wire spacers. A more detailed study, however, indicates that the rotational acceleration is not a major contributing factor to the overall pressure drop through the test section. The higher-than-calculated pressure drop is now attributed to the very high relative roughness of the housing. A new test section is being designed using a special-order smooth scalloped housing and tests with this should show whether the surface roughness of the previously tested housing is causing the high pressure drop.

1.2.3.3 Superheater Heat-Loss to Moderator. A study was performed to determine the effect of reducing the superheater stagnant steam gap. It was assumed that the temperature of the inner reaction of the double-walled tube was at the fluid temperature, and that heat

was lost through radiation and conduction across the gap. The superheater element was divided into several axial sections and the heat loss in each section was computed assuming a constant average temperature. These were summed to give the total heat loss for an element. The results of the computations are shown in Figure 1.17, for both average and hot elements. With a 57-mil gap, the total heat loss to the moderator is 1.65 per cent of the average heat generation in the element. If the gap is reduced to 30 mils, the average element loss is 2.4 per cent of the heat generated in the element. As shown in Figure 1.17, the percentage of heat lost is considerably greater for the hot element than for the average element. Thus, it is not possible to accurately determine the total heat loss to the moderator in the superheater without considering the particular heat generation rate of all fuel elements in the core.

Figure 1.17 Heat loss through double-walled superheater container tube as a function of stagnant-steam gap: (A-C Dwg. 43-024-254)

1.3 FABRICATION OF FUEL FOR CRITICAL FACILITY

The objectives of this project are as follows: 1) to procure fuel and other related materials necessary to conduct experiments to determine and verify nuclear characteristics; and 2) to reprocess and return to the Commission all boiler and high-enrichment superheater fuel used in the critical facility experiments and dispose of all waste materials.

1.3.1 HIGH-ENRICHMENT SUPERHEATER FUEL ELEMENTS

Delivery of all fuel elements and accessories for high-enrichment fuel elements for critical experiments was completed.

1.3.2 LOW-ENRICHMENT SUPERHEATER FUEL ELEMENTS

Inquiries were sent to vendors for 1400 stainless steel clad UO_2 pellet fuel-rods enriched to 3.5 per cent with U-235; 200 fuel rods enriched to 7.0 per cent U-235; 100 fuel pellets enriched to 3.5 per cent U-235; 100 pellets enriched to 7.0 per cent U-235; and 150 spare cladding tubes and end caps. All bids were received and a supplier will be selected during July. Delivery was quoted at four months after the date of order.

1.4 FUEL ELEMENT MANUFACTURING RESEARCH AND DEVELOPMENT

The objective of this project is to perform calculations and studies of methods for producing core subassemblies to determine the most economical manufacturing methods and to obtain maximum performance.

1.4.1 CORE LAYOUTS

The boiler and superheater layouts described in the last quarterly report were approved as the reference design.

1.4.2 BOILER FUEL ELEMENT

1.4.2.1 Reference Design. The reference design for the boiler fuel element is shown in Figure 1.18. The element consists of two fuel-rod bundles housed in a Zircaloy-2 box. The lower bundle, a 9×9 array of fuel rods, is supported and spaced by a cast stainless steel grid, which is fastened with screws to the nozzle assembly. The upper bundle, an 8×8 array of fuel rods, is also supported and spaced by a cast grid, which is fastened to the upper end-fitting with screws. The fuel element is handled by the upper end-fitting, which is designed to accommodate a quick release type lifting tool.

The cladding and box material is Zircaloy-2. The end fittings, fuel-rod support grids, tube-sheet spacers, and fasteners are stainless steel. All springs in the assembly are Inconel "X".

Zircaloy-2 parts were designed to maximum stresses of about 11,000 psi, which is well below the 19,000-psi yield stress at operating temperature. The Inconel "X" springs were designed for stresses less

than 55,000 psi, which is below the 70,000 psi recommended by suppliers. Stresses in the stainless steel parts are not critical but were calculated nonetheless.

1.4.2.2. Fabrication. A prototype of the boiler element (Figure 1.18) has been ordered. It is a duplicate of the current design except that solid stainless steel rods were substituted for the actual fuel rods.

Flow tests are being planned to yield pressure-drop and mechanical-stability data. The prototype will provide an additional means of refining tolerances.

Six of each of the two types of fuel-rod sections were ordered from each of four possible vendors for the Pathfinder boiler core. These rods will be fabricated according to Pathfinder specifications and will be subjected to thorough dimensional and metallurgical inspection. It is anticipated that some of the problems which will occur during fabrication of the core will be encountered during the manufacture of these test rods. The experience gained during this program and the subsequent evaluation of sample rods will aid the fuel manufacturers in predicting problems and in preparing more meaningful bids. In addition, minor design and specification changes may be made if they appear desirable.

1.4.2.3 Mechanical Design. The present reference design is being further refined to permit less stringent tolerances, particularly in the box, and to better satisfy other design requirements.

1.4.2.4 Fuel Element and Component Testing. During the lifetime of a boiler fuel element, there will be differential movement between the nozzle and the grid plate. Therefore, it was desirable to select a nozzle material with desirable wear characteristics when in contact with 304 stainless steel. Several possible materials were selected on the basis of test results reported in the "Corrosion and Wear Handbook". It was further decided to test these materials under the same operating conditions present in Pathfinder.

The test fixture for testing the wear and galling resistance of these materials has been designed, fabricated, and hydrostatically tested to 900 psi. The apparatus is shown in Figures 1.19 and 1.20.

The materials to be tested are Armco 17-4 Ph stainless steel, honed chromium-plated 304 stainless steel, and plain 304 stainless steel. Testing is expected to be completed during the third quarter of 1960.

1.4.3 Superheater Fuel Element

1.4.3.1 Reference Design. The reference design of fuel elements for the first superheater core for Pathfinder is shown in the last quarterly report (Figure 1.15).

1.4.3.2 Fabrication: Prototype High-Enrichment Superheater Fuel Element. A program was initiated to develop and fabricate prototype high-enrichment fuel elements to Pathfinder specifications. The object of this study is to evaluate prospective superheater fuel suppliers on their ability to fabricate fuel elements to Pathfinder specifications with present technology. During this period, a contract was let to the Martin Company for a two-phase program. The first phase is to refine present fabrication techniques and to fabricate several cermet dispersion fuel tubes to Pathfinder specifications. The second phase includes the development of methods of assembling these tubes into fuel assemblies that satisfy design requirements. The program was started on June 3. Work during the past month consisted of materials, equipment, and tooling procurement.

Rare-Earth Burnable Poison Studies for High-Enrichment Superheater Fuel Element. A minor effort was initiated during this period to study and develop a gadolinium oxide burnable poison material for use with the high-enrichment superheater. The object of the investigations is to develop a gadolinium-oxide dispersion in a ceramic matrix with closely controlled particle size.

(continued on page 46)

Figure 1.19 Nozzle-galling test assembly. Code: 1) Flange, 2) Clevis bracket for nozzle sleeve, 3) nozzle sleeve, 4) test housing, 5) nozzle assembly, 6) shaft, 7) flange assembly, 8) bracket assembly, and 9) air cylinder. (A-C Dwg. 43-500-775)

Figure 1.20 Nozzle-galling test assembly.

Preliminary studies will include investigations of sintering characteristics of the oxide. Gd_2O_3 will be sintered into compacts, the compacts will be ball-milled, and sized with sieve. The object of this work will be to determine the feasibility of fabricating sintered oxide particles of a closely controlled size.

During the quarter, a sieve-analysis of gadolinium oxide was made. Powder appeared to consist of particles of -400 mesh size (less than 35 micron diameter) but a more detailed sieve analysis was not possible because the powder tended to agglomerate on top of some of the screens. This may have been due to the hygroscopic nature of the powders.

A sub-sieve analysis was also made on the powder and results showed an average particle diameter of 0.925 to 0.930 microns in two runs. This substantiated the results of the sieve analysis.

Pelleting studies were also initiated during the quarter. Pellets of 0.405 in. dia. and 0.405 in. height were formed at pressures from 5 to 50 tsi. Sufficient green strength for handling and sintering were achieved at 26 tsi. Two to four weight per cent of carbowax was added as binder-lubricant. These pellets had green densities of about 60 per cent of theoretical density.

No successful sinters of these pellets have been made. Green pellets air sintered at 2700 F either reacted with containers or fractured from thermal stress or shock into small pieces.

A study of possible reactions between the oxide and the aluminum oxide matrix material is being made. Two pellets consisting of alternate layers of alumina and Gd_2O_3 and two pellets consisting of a mixture with an 80-to-20 volume ratio of alumina and gadolinium oxide were sintered at 2700 F. The occurrence of reactions will be determined by metallographic studies, and possibly through x-ray diffraction.

A supporting study of alumina pelleting characteristics was initiated. This consists of a pressure-time study of green forming and strength. A sieve analysis of as-received aluminum oxide was made and results are given in table 1.10.

Four pellets were pressed at 30 and 40 tsi using as-received Al_2O_3 with 2 w/o Carbowax. Green densities of these pellets ran from 62.4 to 65 per cent of theoretical density. Further pelleting and sintering studies will be made during the next quarter.

Table 1.10. SIEVE-ANALYSIS FOR AS-RECEIVED ALUMINA

Particle Mesh Size	Particle Diameter Range	Percent of Powder*
+70	88 and larger	19.9
-170+200	74 to 88	25.3
-200+230	62 to 74	23.9
-230+270	53 to 62	11.2
-270+325	44 to 53	10.9
-325+400	35 to 44	1.8
-400	44 and smaller	4.7

* Using a 100 gram sample with a loss of 2.3% during sieving.

1.4.3.3 Mechanical Design. Stress Analysis of the High-Enrichment Superheater Fuel Element. The high-enrichment superheater fuel tubes, inner container tube, poison rod, and spacer wires were analyzed for stresses, creep, thermal fatigue, thermal expansion, and thermal bowing under reactor operating conditions. An analysis was also made to determine the degree of thermal shock that could be tolerated without causing cracks or permanent deformation of the superheater element. This thermal shock would result from flooding the superheater during shutdown.

Thermal Stress of Fuel Tubes. Thermal stress due to heat generation was calculated with the equation for a flat-plate, sandwich-type, fuel element*:

$$\sigma_y = \sigma_z = \frac{\alpha E Q a^2}{2k(1-\nu)} \left[1 + \frac{c}{a} - \frac{2a}{3(a+c)} \right] \quad (1.1)$$

where, Q = volumetric heat generation

a = one-half the fuel thickness, and

c = clad thickness.

This equation is valid for the fuel tubes because the tube wall thickness is small compared to the radius of the tube.

* Glasstone, "Principles of Nuclear Reactor Engineering," P. 709, 1955

The thermal stress in fuel tubes due to axial temperature gradient was calculated by the equation,*

$$\sigma_z = \frac{2(0.329\sqrt{R_o t})}{(1-\nu^2)^{3/4}} \frac{\Delta T}{\Delta Z} \quad (1.2)$$

where,

R_o = the mean radius of the tube, and

t = the wall thickness.

The maximum axial temperature gradient, $\Delta T/\Delta Z$, is 14 F/in.

Eqs. (1.1) and (1.2) gave a maximum tensile stress at the outer cladding surface of 570 psi for the inner fuel tube and 600 psi for the outer fuel tube.

Pressure Stress of Fuel Tubes. The stress in the fuel tubes due to a pressure difference between flow channels was calculated using the equation,**

$$\sigma_t = \frac{p_1(r_1^2 + r_2^2) - 2p_2r_2^2}{r_2^2 - r_1^2} \quad (1.3)$$

where,

σ_t = maximum circumferential stress at inside surface,

p_1 = internal pressure,

p_2 = external pressure,

r_1 = inside radius,

r_2 = outside radius.

* Timoshenko, "Theory of Plates and Shells," p. 425, 1940.

** Seely and Smith, "Advanced Mechanics of Materials," p. 299, 1959

Assuming the maximum pressure difference between flow channels is 10 psi ($p_1 = 590$ psi; $p_2 = 600$ psi), the maximum stress is -750 psi in the outer fuel tube and -480 psi in the inner fuel tube. Assuming the same maximum pressure difference between flow channels with $p_1 = 600$ psi and $p_2 = 590$ psi, the maximum stress is -450 psi in the outer fuel tube and -380 psi in the inner fuel tube. The minus sign denotes compressive stress.

Circumferential Stress due to Spacer Wires. Equations were derived for calculating the maximum circumferential stress in the tube wall due to forces caused by the wire spacers. With the three spacers spaced 120 degrees apart exerting equal forces, the maximum stress is given by the equation,

$$\sigma = \frac{Et(\Delta R)}{0.1687R_m^3} \quad (1.4)$$

where,

E = Young's modulus

R = the change in tube radius caused by the forces,

t = wall thickness, and

R_m = mean radius of the tube.

This gives a stress of $22,000 \times 10^3 \times \Delta R$ psi for the outer fuel tube, and $39,000 \times 10^3 \times \Delta R$ psi for the inner fuel tube. The high stress values indicate that the clearance between tubes must allow for differences in radial thermal expansion at operating temperatures, to prevent permanent deformation of fuel tubes at the spacers.

Thermal Bowing. Thermal bowing of fuel tubes due to linear temperature difference, ΔT , across the tube diameter will be restricted by the wire spacers. The reaction at the wire will cause circumferential bending stress in the tube wall. Considering the total force to be applied at one point at the center of the tube length, the maximum stress is given by the equation,

$$\sigma = 27.0 \frac{\alpha \Delta T}{D_o} \frac{E}{L} \frac{R_m^4}{t} \quad (1.5)$$

where,

α = mean coefficient of thermal expansion

D_o = the outside diameter of the tube, and

L = the tube length.

This gives a stress of 36.0 psi/°F for the inner and 90.0 psi/°F for the outer fuel tube.

The maximum longitudinal bending stress caused by restraining the thermal bowing is given by the equation,

$$\sigma = \frac{3}{4} (\alpha E \Delta T) \quad (1.6)$$

This gives a stress of 150 psi/°F in the inner and outer fuel tubes.

The yield strength of 316 stainless steel is 22,000 psi at 900°F.

This indicates that the tubes should be able to stand a temperature difference of 100°F across the diameter, which is much larger than is expected during operation of Pathfinder.

Differences in axial thermal expansion between the spacer-wires and fuel tubes will cause a compressive axial stress in the fuel tubes. The axial load on the tubes was calculated by the equation,

$$(\alpha L \Delta T + \frac{PL}{AE})_{tube} = (\alpha L \Delta T + \frac{PL}{AE})_{wire} \quad (1.7)$$

This equation gave a maximum compressive stress, $\sigma = P/A$, of 10,000 psi for the inner fuel tube and 5,200 psi for the outer fuel tube. This stress will be quickly relieved, however, due to creep of the wire spacers, which have much higher stresses.

The maximum radial temperature drop across the inner superheater container tube wall during reactor operation was calculated to be 2.0 F for a wall thickness of 0.020 in. and 1.0 F for a wall thickness of 0.010 in. Using the equation,*

$$\sigma_{\theta} = \sigma_z = \frac{\alpha E \Delta T}{Z (1 - \nu)} \quad (1.8)$$

to calculate the stress due to the radial temperature difference and Eq. 1.2 to calculate the stress due to the axial $\Delta T/\Delta Z$ of 10 F/in gave a maximum tensile stress at the outer surface of the tube wall equal to 250 psi for the wall thickness of 0.01 in. and 440 psi for a wall thickness of 0.02 in.

With the reactor operating at 100 per cent power, the average pressure drop through the superheater is 70 psi and at 150 per cent full power, the pressure drop is approximately 155 psi. This pressure

* Timoshenko and Goodier, "Theory of Elasticity," p. 414, 1951

drop causes circumferential stress in the inner superheater container tube. Using Eq. 1.3, with a pressure drop of 70 psi, the maximum tensile stress at the inside surface of the tube wall is 1,070 psi for a wall thickness, t , of 0.020 in. and 2,700 psi for a wall thickness of 0.010 in. With a pressure drop of 155 psi, the maximum tensile stress is 3,070 psi for a 0.020-in thick wall and 6,630 psi for a 0.010-in thick wall.

The axial load on the inner superheater container tube caused by the differential thermal expansion between the tube and wire spacers was calculated by Eq. 1.7. This gave a maximum compressive axial stress in the 0.886-in. i.d. tube section of 9,600 psi for a 0.020-in thick wall and 6,580 psi for a 0.010-in thick wall. This stress will be relieved by creep of the wire spacers. The circumferential stress caused by the spacers in restricting bowing or radial thermal expansion will be less than that in the fuel tubes.

The temperature drop across the cladding of the poison rod will be negligible; therefore, there will be no stress caused by a radial temperature difference. The axial temperature gradient, $\Delta T / \Delta Z$, produces a maximum of less than 100 psi.

The poison rod cladding will be subjected to an external pressure of approximately 600 psi, which causes compressive stress. The wall thickness required to prevent inelastic failure was calculated from the equation,

$$q_{cr} = \frac{E}{4(1-\nu^2)} \left(\frac{t}{R_m} \right)^3 \quad (1.9)$$

where,

R_m = the mean radius,

t = the wall thickness

q_{cr} = critical pressure.

This equation gave a wall thickness of 0.010 in., but a factor of safety should be applied to this to account for imperfections in the tube. With a wall thickness of 0.010 in. and a critical pressure of 600 psi, the compressive stress in the wall is 13,000 psi.

The spacer wires that will be attached to the poison rod will be approximately the same temperature as the rod; therefore, they will not cause any axial stress. Any circumferential stress caused by the spacers will be approximately the same as that in the fuel tubes.

The wire spacers that are attached to the fuel tubes and inner insulating tube will be stressed due to differences in axial thermal expansion between the tubes and the wires. The load on the wires was calculated from Eq. 1.7. This gave a maximum stress in the wires of 90,000 psi.

Table 1.11 is a summary of the maximum stresses due to temperature and pressure differences in each part of the superheater element during reactor operation:

Table 1.11 SUMMARY OF MAXIMUM STRESSES IN SUPERHEATER FUEL ASSEMBLY AND CONTAINER TUBE

	psi
Spacer Wires, tension	90,000
Poison Rod, compression	13,000
Inner Container Tube, tension	7,000
Inner Fuel Tube, outer surface, (tension)	570
Inner Fuel Tube, inner surface, (compression)	840
Outer Fuel Tube, outer surface, (tension)	600
Outer Fuel Tube, inner surface, (compression)	750

The maximum stresses in the fuel tubes, inner container tube, and poison rod are below that required to produce a creep rate in 304 or 316 stainless steel of 1 per cent in 10,000 hours; therefore, creep should not cause any problem in the superheater tubes. The spacer wires have higher stresses; but these will be relieved by creeping without affecting reactor operation.

Thermal cycling during reactor operation will not cause fatigue failure because the stress is much below the yield strength of the material at operating temperature. Assuming a thermal cycling of 50°F, and 1 cps, the alternating stress in the tubes calculated from Eq. 1.8 is 8,300 psi. The yield strength at the average operating temperature is 20,000 psi. The curve of allowable alternating stress versus number of cycles as given in ASME paper 58-Met-1 indicates that an infinite number of cycles can be accommodated at more than twice the operating stress level.

The axial and radial thermal expansion of the superheater tubes was calculated to determine the stresses due to the spacer wires and the radial clearance between tubes during reactor operation. The radial clearance between tubes varies along the tube length due to the axial temperature distribution. The effect that this may have on the heat transfer characteristics of the element will be investigated further.

The amount of thermal bowing that could occur due to temperature differences across the diameter of the superheater tubes was determined for various temperature distributions. The maximum thermal bowing

occurs with a linear temperature difference across the tube diameter, which is constant along the length of the tube. This case was investigated to determine the effect of the number of spacers on the maximum deflection.

The equation used to determine the deflection is,

$$y = \int \frac{(d\Delta T)x}{D_o} dx + \iint \frac{M dx^2}{EI} \quad (1.10)$$

The maximum allowable temperature difference across the inner fuel tube ($D_o = 0.600$ in) with spacers every 6 in. is 13°F for a maximum deflection of 0.001 in. This indicates that the tubes should be supported along their entire length with continuous spacers.

Thermal Shock. Part of the reference shut-down procedure for Pathfinder includes flooding of the superheater. It is the purpose of this phase of the overall analysis of the superheater section to critically evaluate the flooding procedure with the ultimate goal of insuring that the operating integrity of the fuel element is not adversely affected.

A thermal shock program was initiated to determine if any limitations must be placed on the flooding operation presently being considered. An extensive literature survey was made of all material available on thermal shock and thermal cycling fatigue. From this

survey it was found that the most applicable analytical method was reported by Coffin.*

Assuming a fully ductile material, a fatigue life of 1000 cycles was calculated using a thermal shock of 1000°F and conservative heat transfer coefficients. From evidence thus far accumulated, a reduction in ductility is anticipated due to sigma-phase formation, irradiation damage, and corrosion. However, this reduction in ductility, will never result in a totally embrittled material. The extent of the loss of ductility is being investigated.

1.4.3.4 Superheater Container Tubes Seal Test. Testing of seals to be used between the inner and outer superheater container tubes continued. The aluminum force-fit seals described in the last quarterly report were retested, and thermal cycled. After a few cycles, the seal began to leak considerably. This indicated that the aluminum had evidently yielded during the previous cycle.

Aluminum O-rings were also tested similarly to the stainless steel O-ring seals described in the last quarterly report. Considerable difficulty was encountered in keeping the O-rings from becoming scratched during insertion. These scratches caused unsatisfactory sealing.

In order to develop the final seal design to be used in the core, a seal test was designed to operate at reactor temperature and pres-

*L. F. Coffin, "Thermal Stress Fatigue", Product Engineering - June 1957

Figure 1.21 Superheater seal tester assembly. (A-C Dwg. 43-002-150).

sure. This high-pressure seal test apparatus is shown in Figure 1.21. Based on data gathered from the low-pressure tests, seals that resemble the final design both in configuration and fabrication can be tested. The test will accommodate nine seals simultaneously at identical conditions for long periods of time. The housing from the nozzle galling test will be used as the pressure vessel. Steam will be generated within the housing itself and pressurized nitrogen will provide the back pressure on the seals.

1.5 NUCLEAR HANDLING TOOLS

The objective of this project is to perform conceptual engineering and experimental work as required for development of simplified but reliable fuel handling tools and special tools for repairing or replacing reactor parts.

1.5.1 BOILER FUEL HANDLING TOOLS

A new boiler fuel handling tool is being designed using a quick release type pin. This tool will latch onto the fuel element in a vertical position.

Preliminary work was started on equipment for underwater assembly and disassembly of the Zircaloy-clad boiler element. These operations will be necessary in order to re-use the Zircaloy-2 box.

1.5.2 SUPERHEATER FUEL HANDLING TOOL

An improved version of the long-handled superheater element handling pole was designed and is shown in Figure 1.22. This tool, will be bouyant over most of its 19-ft of useful travel. The

Figure 1.22 Superheater fuel element handling tool.
(A-C Dwg. 43-002-166)

pole utilizes the handling tool type-two shown in the last quarterly report. A prototype of this pole is being built for testing with the core mock-up in the 40-ft deep handling tank.

The basket for holding groups of superheater elements was modified and now receives the element readily.

Inquiries for two kinds of underwater periscopes were sent out and three quotations were received. One periscope will probably be necessary for operating the underwater boiler element assembly equipment. The other is needed for inspection of spent elements, used boxes, control rods, etc.

1.6 LOW-ENRICHMENT SUPERHEATER FUEL ELEMENT

The objectives of this project are as follows:

- 1) to perform calculations optimizing nuclear and thermal characteristics of a low-enrichment superheater fuel element.
- 2) to evaluate fabrication and testing techniques that will lead to the lowest cost superheater fuel possible within limits of Pathfinder Plant design and operating reliability.
- 3) to attempt to get as much information as is required for early insertion of a low-enrichment superheater core in Pathfinder.

1.6.1 DESIGN STATUS

A research and development program is being conducted to develop a low-enrichment superheater fuel element for Pathfinder. The initial phase of this program has included a study of fuel and cladding materials, fabrication processes, design, heat-transfer, irradiation

damage, and associated operational characteristics. Primary emphasis has been placed on three fuel configurations using uranium dioxide fuel with stainless steel cladding. In one concept, seven fuel rods in a septafoil cluster are contained in a scalloped or round flow tube. The second concept is a single annular fuel tube. The third concept consists of a perforated fuel rod penetrated by multiple flow channels.

Tests with the seven-pin fuel rod are being made to determine pressure drop, and were discussed in Section 1.2. Present tests are using air, but tests are planned in the heat transfer loop in which steam will be used. Other tests will be planned and conducted as development proceeds.

1.6.2 FABRICATION DEVELOPMENT

Concerted effort is now being directed toward the development of an improved swage-compacted UO_2 fuel element of the septafoil design. The object of current work is to develop a method of fabricating high density swage-compacted UO_2 fuel rods with limited interconnected porosity for good heat transfer and retention of gaseous fission products, and to assemble these rods into an assembly capable of performing as required.

1.6.2.1 Programmatic Data Survey. An enlarged study is now being made of available data applicable to the program. Primary emphasis has been placed on methods of swage-compacting, properties of swage-compacted fuel rods, selection of oxide grades for swaging, and irradiation damage to UO_2 fuel elements. Twenty-three reports with applicable data were reviewed and evaluated during the quarter. About half of these concerned swage-compacting techniques and the properties of swage-compacted fuel elements.

1.6.2.2 Ceramic Pellet Fabrication. A minor effort was continued on uranium dioxide pellet fabrication. Using the best pressing force, binder, and particle size characteristics developed to date, thirty-eight new pellets were pressed and sintered to determine density variations for constant powder and pressure variables. All thirty-eight pellets were prepared from 64-hour ball-milled Shattuck depleted UO_2 containing 2.0 weight per cent carbowax, and were pressed at 70 tsi pressure. Results are given in Table 1.12.

The thirty-eight pellets had an average green density of 72.25 ± 0.25 per cent of theoretical. After sintering at approximately 1700 C for 15 hr these pellets had a density of 90.40 ± 3.0 per cent of theoretical, which was a much wider density range and a lower average density than had been previously obtained. Examination showed a large number of micro-cracks within the pellets. This cracking apparently resulted either from failure to bake out the Carbowax prior to firing or from thermal shock when being placed

TABLE 1.12

UO₂ PELLET PROPERTIES, SERIES E - 64 HOUR BALL MILLED DEPLETED OXIDE
Group 9 - 2.0 per cent Carbowax, sintered 15 hours at 1700°C.

Forming Pressure (tsi)	Green Density		Sintered Density		Forming Pressure (tsi)	Green Density		Sintered Density	
	(g/cc)	Per Cent Theoretical (Dimensional)	(g/cc)	Per Cent Theoretical (Pycnometer)		(g/cc)	Per Cent Theoretical (Dimensional)	(g/cc)	Per Cent Theoretical (Pycnometer)
70	7.917	72.17	10.23	93.26	70	7.951	72.48	9.915	90.39
70	7.898	72.00	10.13	92.35	70	7.942	72.40	9.926	90.49
70	7.913	72.13	9.987	91.04	70	7.918	72.18	10.22	93.17
70	7.902	72.03	10.06	91.71	70	7.908	72.09	10.03	91.43
70	7.922	72.22	9.991	91.08	70	7.926	72.25	9.721	88.62
70	7.920	72.20	9.879	90.06	70	7.914	72.14	9.807	89.40
70	7.924	72.24	9.833	89.64	70	7.913	72.13	9.782	89.17
70	7.927	72.26	9.854	89.83	70	7.916	72.16	9.925	90.48
70	7.944	72.42	9.769	89.05	70	7.924	72.24	9.993	91.10
70	7.927	72.26	9.818	89.50	70	7.904	72.05	10.00	91.16
70	7.944	72.42	9.859	89.87	70	7.917	72.17	10.27	93.62
70	7.947	72.45	10.08	91.89	70	7.928	72.27	10.06	91.71
70	7.957	72.54	10.04	91.52	70	7.922	72.22	9.768	89.05
70	7.937	72.35	9.952	90.72	70	9.914	72.14	10.02	91.34
70	7.923	72.23	9.903	90.28	70	7.918	72.18	9.637	87.85
70	7.930	72.29	9.878	90.05	70	7.918	72.18	9.774	89.10
70	7.936	72.34	9.846	89.76	70	7.922	72.22	9.784	89.19
70	7.945	72.43	9.720	88.61	70	7.903	72.04	9.874	90.01
70	7.949	72.46	9.808	89.41					
70	7.939	72.37	9.797	89.31					

in the furnace with subsequent failure of cracks to heal during sintering. No further studies of this type are planned until an automatic program controller is attached to the hydrogen-atmosphere furnace. This equipment will enable close control of maximum temperature, heating, and cooling rates, and the outgassing procedure used to remove the organic binders from the pellets.

During the quarter, a metallographic analysis was initiated on porosity and grain structure in pellets sintered during the program. The objectives of this study are to evaluate pycnometer-density measurements with microscopic observations; to evaluate the grain size of the sintered pellets; to study the connection between porosity pits; and to study the variation in density throughout the pellets.

The grain size in the sintered pellets studied ranged between 15 and 25 microns in diameter. Some internal cracking was observed which appeared to have partially healed during sintering. No direct comparison has been obtained between pycnometer-density measurements and metallographic determinations of porosity volume, presumably because the periphery of the pores is lost while preparing specimens for inspection. The porosity within the pellets is of the non-interconnected type well isolated by diffusion and grain growth. Figure 1.23 shows the surface microstructure of a UO_2 pellet. Further work will be continued on this study.

In another phase of pellet fabrication, an investigation was made of the effects of furnace temperature on the thermal shock

properties of green and sintered pellets. Sintered UO_2 pellets were subjected to thermal shock by removing them from the furnace at temperatures between 900 and 1650 C without causing serious fracture or large changes in density. One-per-cent change or less in theoretical density was encountered but general structural integrity and surface appearance were not altered.

The thermal shock resistance of green pellets was also investigated. For this study, twenty-two pellets were pressed at 60 to 70 tsi using established techniques. Outgassing studies in the preheating region of the furnace showed the feasibility of removing the

Figure 1.23 Uranium dioxide sintered pellet, unpolished surface structure. Magn: 800X (NPD Photo 12-A-0-1)

organic binder at 450 C while the hot zone of the furnace is held at about 1300 C. Subsequent thermal shock treatments on the out-gassed pellets between 700 and 1000 C failed to establish the maximum temperature before thermal shock.

Further studies will be made to select the maximum permissible temperatures for placing green pellets into the furnace for sintering, and their subsequent removal.

1.6.2.3 Swage-Compacting Studies. A major effort was initiated during the quarter towards the development of a swage-compacted uranium oxide fuel element for superheater applications. The object of the study is to prepare a high-density UO_2 fuel rod with limited interconnecting porosity for retention of fission product gases and with good heat-transfer properties. Primary emphasis is directed towards a finished diameter around 0.225 in. for assembly into the septafoil design.

The initial phase of the program includes laboratory investigations of swage-compacting parameters. The seven variables under study are: uranium dioxide grade, particle size of powder, particle size distribution, particle density, tap density of pre-swaged tube, amount of reduction and the number of passes in swaging, and the strength of the sheath material.

Aluminum Oxide Studies. Initial control experiments were made with aluminum oxide and 304 and 316L stainless steel tubing. The object of these studies was to evaluate performance of the swaging dies and machine, the working characteristics of the stainless steel sheath during swaging, and the density of the swaged alumina. As-received Al_2O_3 was used in these studies with the properties given in Table 1.13.

The procedure used to prepare the rods was as follows:

- The tube was cut to length.
- The inside and outside of the tube were cleaned.
- One end of the tube was plugged with steel wool.
- The plugged end was crimped.
- The tube was filled with as-received alumina.
- The alumina was vibratory-packed for 15 minutes.
- The open end was plugged with steel wool and crimped.
- The crimped ends were welded.
- The rod was swaged according to schedule.

Table 1.13 SIEVE ANALYSIS OF ALUMINUM OXIDE *

Particle Size in Mesh	Particle Diameter Range in Microns	Percent of Powder
+170	88 and larger	19.9
-170 +200	74 to 88	25.3
-200 +230	62 to 74	23.9
-230 +270	53 to 62	11.2
-270 +325	44 to 53	10.9
-325 +400	35 to 44	1.8
-400	35 and smaller	4.7

* Using a 100 gram sample with a loss of 2.3 per cent during sieving

With a constant particle-size range of alumina, the reduction per pass was varied to select the optimum swaging method. Primary objective was to obtain high-quality swaged cladding with secondary objective of obtaining high swaged density. A qualitative visual evaluation was made of the cladding surface; and the inside surface was observed microscopically.

The reduction in cross sectional area could be varied by altering the number of die passes, the die size, and the original size of the stainless steel sheath. In some specimens, the stainless steel cladding was annealed between swaging passes to remove cold-working effects and soften the material for further reductions. The annealing time and temperature varied, and the external surface of the cladding was badly oxidized due to a poor annealing atmos-

phere. The furnace will be fitted with a bright-annealing hydrogen atmosphere for proper heat treating of stainless steel before this is attempted again.

Specimen variables and properties are given in Table 1.14. Seven different reductions were used. All final diameters varied slightly from the die diameter because tolerances could not be accurately controlled by shims.

The density of the four rods varied between 49 and 75 per cent of the theoretical density for Al_2O_3 . The highest density, and the largest range was obtained with a three-pass reduction schedule. No serious attempt was made to improve these values. No measurements were made on the tap densities of the as-loaded tube prior to swaging.

A considerable variation was encountered in the condition of the stainless steel sheath. Most of the fifteen specimens had a good surface finish. However, some of the tubes were creased and/or cracked on the outside surface due to the reduction process. It is felt that some of these surface defects were due to the difficulty of manually feeding the die in a proper manner.

Examination of the inside surface of the steel cladding gave a more precise evaluation of the effects of this cold-working operation. Metallographic preparation and analysis of eight of the specimens has been completed. Figures 1.24 through 1.33 give microscopic evidence of the effects of swaging on the sheath. Con-

Table 1.14 SWAGE-COMPACTION STUDIES OF Al_2O_3

Sample No.	Starting Diameter in inch.	Reduction Passes				Final Diameter in inch.	Density Percent Theoretical	Clad Condition*	
		1st	2nd	3rd	4th			OD	ID
JK-1	0.325	0.250a	0.225	----	----	0.218	69-75	G	SR
JK-2	0.325	0.250a	0.225	----	----	0.218	----	G	---
JK-3	0.325	0.250a	0.225	----	----	0.218	----	G	---
JK-4	0.325	0.275	0.250	0.235a	0.225	0.218	----	C	BC
JK-5	0.325	0.275	0.250	0.235	0.225	0.218	----	C	BC
JK-6	0.325	0.275	0.250	0.235		0.228	----	G	SR
JK-7	0.325	0.275	0.250	----	----	0.246	----	G	G
JK-8	0.325	0.250	0.225	----	----	0.219	----	G	G
JK-9	0.325	0.275	----	----	----	0.267	----	K	---
JK-10	0.325	0.275	0.250	----	----	0.246	49	K	C
JK-11	0.325	0.275	0.250	0.235	----	0.230	68	G	---
JK-12	0.325	0.275	0.250	0.235	0.225	0.220	71	G	---
JK-13	0.325	0.225	----	----	----	0.218	----	CK	---
JK-14	0.325	0.225	----	----	----	0.218	----	G	---
JK-15	0.325	0.225	----	----	----	0.218	----	G	---

a Specimens annealed in H_2

* Index for Clad Condition Legend

G = good
 C = cracked
 K = creased
 R = rough
 SR = slightly rough
 BC = badly cracked

Figure 1.24 316L Weld drawn stainless steel tubing, as-received.
Magn: 75X (NP Photo 15A-9-1)

Figure 1.25 316L Weld drawn stainless steel tubing, as-received.
Magn: 600X (NP Photo 15A-9-2)

Figure 1.26 Swaged specimen JK-1, transverse section. Magn: 150X
(NP-Photo 15A-11-1)

Figure 1.27 Swaged specimen JK-1, longitudinal section. Magn: 150X
(NP Photo 15A-11-2)

siderable cracking was encountered in this first set of specimens.

Due to the high percentage of coarse particle sizes in the as-received powder, localized thinning and thickening of the walls was common.

Of the fifteen samples run (Table 1.14), samples 1, 2, 3, and 8 were of the highest quality. These samples used a 0.325 in. o.d. stainless steel sheath. After loading the rod was passed successively

Figure 1.28 Swaged specimen JK-4, transverse section (Notice deep cracking) Magn: 150X (NP Photo 15A-5-1)

Figure 1.29 Swaged specimen JK-5, transverse section. (Notice deep cracking) Magn: 150X (NP Photo 15A-6-1)

Figure 1.30 Swaged specimen JK-6, transverse section. Magn: 150X (NP Photo 15A-7-1)

Figure 1.31 Swaged specimen JK-7, transverse section. Magn: 150X (NP Photo 15A-8-1)

through 0.250- and 0.225-in dies to a finished diameter of about 0.218 in. o.d.. This resulted in a 55-percent reduction in cross-sectional area. This schedule was then standardized for the next series of experiments. The successive reduction steps of Sample 4 are shown in Figure 1.34.

Figure 1.32 Swaged specimen JK-8 transverse section. Magn: 150X (NP Photo 15A-10-1)

Figure 1.33 Swaged specimen JK-10 transverse section. (Notice cracking) Magn: 200X (NP Photo 15A-12-5)

Figure 1.34 Swage-compacted Al_2O_3 rods in various stages of reduction. (NP Photo 15A-10-2)

Uranium Dioxide. The next series of experiments were made with uranium dioxide and 316L stainless steel weldrawn tubing. In order to standardize some of the seven variables previously listed for the swage-compaction study, only Spencer Chemical Company's swageable grade of fused oxide was used with the best swaging schedule that

Table 1.15 SIEVE ANALYSIS OF FUSED UO_2

Particle Size in Mesh	Particle Diameter Range in Microns	Percent of Powder*
+70	210 and larger	71.8
-70+100	149 to 210	7.5
-100+140	105 to 149	5.4
-140+170	88 to 105	1.1
-170+200	74 to 88	2.9
-200+230	62 to 74	2.1
-230+270	53 to 62	2.2
-270+325	44 to 53	2.4
-325+400	35 to 44	1.2
-400	44 and smaller	2.8

* 0.6% sample loss during sieving.

has been thus far established. The sieve analysis of the UO_2 is given in Table 1.15.

The same basic loading procedure used for the Al_2O_3 studies was used in these experiments. However, the particle size and particle size distribution of UO_2 was altered, and the as-loaded tap density was measured prior to swage compacting. The tap density measurements were made by loading a five milliliter graduated cylinder with oxide powders, vibrating the cylinder one-half hour, and then calculating the density from the measured weight and volume of UO_2 . The results of the tap density measurements are given in Table 1.16.

The as-received oxide yields the highest tap density and would therefore be expected to yield the highest swaged density with the smallest reduction in cross-section area. These studies will be

Table 1.16 TAP DENSITY OF UO_2

Particle Size in Mesh	Particle Diameter Microns	Density g/cc	Density Percent of Theoretical
As-Received	(see Table 4)	6.5	59.2
-400	35 and smaller	5.3	48.3
-325+400	35 to 44	5.0	45.6
-170+325	44 to 88	5.1	46.5
-140+325	44 to 105	6.0	54.7
-100+325	44 to 149	5.5	50.1
-70+170	88 to 210	5.8	52.9
-70+140	105 to 210	6.2	56.5
-70+100	149 to 210	5.3	48.3
-170+325	44 to 88	6.0	54.1 (see note a)

a. In this tube a weighted rod was used above the powder bed to increase the density during packing and vibration.

continued to determine the optimum particle size and size range distribution for the highest ultimate tap density. Increased use will be made of tap-packing rods during vibration.

Table 1.17 lists the variables and properties of nineteen UO_2 swaged rods completed or now in process. All rods contained Spencer swageable grade oxide in 0.325-in o.d. stainless steel tubing. All swaging consisted of the same two-pass schedule; one pass through the 0.250-in. die and one pass through the 0.225-in. die. Total reduction in cross-sectional area for these rods was 54 per cent.

Swaging of the first six samples was completed. Measured values ranged from 9.1 to 10.1 g/cc or 83 to 92 per cent of theoretical. These values compare well with similar data reported in the literature.

Table 1.17 SWAGE-COMPACTION STUDIES OF UO_2

Sample No.	Powder Size in Mesh	Starting Diameter in Inch.	Reduction Passes			Tap Density Per Cent Theoretical	Final Diameter	Density % of Theor.			Cladding Surface O.D.
			<u>1st</u>	<u>2nd</u>	<u>3rd</u>			<u>I</u>	<u>M</u>	<u>B</u>	
JK-A1	A.R.*	0.325	0.250	0.225		65	0.220	89	82	86	VG
JK-A2	A.R.	0.325	0.250	0.225		65	0.219	92	90	91	VG
JK-A3	-70+200	0.325	0.250	0.225		--	0.220	90	90	90	VG
JK-A4	-70+200	0.325	0.250	0.225		--	0.220	90	90	89	VG
JK-A5	-100+325	0.325	0.250	0.225		50	0.219	90	90	90	VG
JK-A6	-100+325	0.325	0.250	0.225		50	---	89	90	90	VG
JK-A7	-400	0.325	0.250	0.225		48	0.220				
JK-A8	-400	0.325	0.250	0.225		48	0.220				
JK-A9	-325+400	0.325	0.250	0.225		46	0.221				
JK-A10	-170+325	0.325	0.250	0.225		47	0.220				
JK-A11	-170+325	0.325	0.250	0.225		47	0.221			I	
JK-A12	-140+325	0.325	0.250	0.225		55	---				
JK-A13	-140+325	0.325	0.250	0.225		55	0.221				
JK-A14	-70+170	0.325	0.250	0.225		53	0.221				
JK-A15	-70+170	0.325	0.250	0.225		53	---				
JK-A16	-70+140	0.325	0.250	0.225		57					
JK-A17	-70+140	0.325	0.250	0.225		57	I				
JK-A18	-70+100	0.325	0.250	0.225		48					
JK-A19	-70+100	0.325	0.250	0.225		48					

* A.R. = As-Received Powder

VG = Very good

I = Incomplete data

The outside surface of these UO_2 rods looked very good. Some samples contained minor scratches and crimping of the surface due to poor manipulation during swaging. A swaging jig which will automatically feed the rods into the swager is now being made.

Wall thickness variations versus particle size and distribution is now receiving the most attention. Figure 1.36 is a photomicrograph of one of the swage-compacted clad tubes, which used as-received UO_2 powder. Notice the effect of the large UO_2 particle on the wall thickness. Table 1.18 gives data obtained in metallographic analysis of wall-thickness variations on the first six UO_2 rods of this series.

The photomicrographs of the last swage-compacted UO_2 rods have not been completed, but they will be included in the next quarterly report. It may be observed from the data of Table 1.18 that finer mesh oxide gives less variation in wall thickness. The effect of large particle size is very evident in Figure 1.35.

Table 1.18 WALL THICKNESS VARIATIONS OF UO_2 RODS

Sample No.	Average Wall Thickness* (in.)	Deviations	
		Maximum	Minimum
JK-A1	0.0095	+0.0034	-0.0016
JK-A2	0.0102	+0.0012	-0.0009
JK-A3	0.0111	+0.0013	-0.0009
JK-A4	0.0107	+0.0011	-0.0011
JK-A5	0.0111	+0.0007	-0.0015
JK-A6	0.0109	+0.0008	-0.0010

* Averages from 36 readings per sample.

Figure 1.35 Swaged specimen JK-12, transverse section. Notice effect of large UO_2 particle on clad.
Magn: 150X (NP Photo 15A-14-1)

Further work is continuing on swage-compacted UO_2 rods. Future plans include;

Continued evaluation of particle size and distribution on wall thickness variations and ultimate swaged density.

Methods of increasing tap density of the pre-compacted rod.

Variation in quantity and number of reduction passes.

Evaluation of ductility of pre-compacted clad tube.

Evaluation of particle density, size, and distribution of other grades of UO_2 .

Investigation of methods of increasing the ultimate density of the swage-compacted fuel rod by alternate consolidation processes.

Work planned for the next period includes study of fuel-rod end caps, transition fittings, and spacer components for the low-enrichment fuel element concept.

2. REACTOR MECHANICAL STUDIES

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2.1 VESSEL AND STRUCTURES

The objectives of this project are as follows: 1) to perform extensive studies, conceptual engineering, design evaluations, and tests relative to the reactor vessel and structures, so as to establish reliable, safe, and improved designs for the vessel and structure; and 2) to perform investigations and tests to insure proper hydraulic design.

2.1.1 REACTOR VESSEL SHELL

All major components for the shell were received. The fabrication schedule in the Allis-Chalmers shops was accelerated to compensate for the late delivery of the large hemispherical shell section.

2.1.2 FULL-SCALE CLOSURE TEST

Initial testing of the full-scale closure was begun. The corrugated stainless-steel-jacketed soft-iron-core gaskets were successfully tested to 700 psig. Initial strain-gage data established the adequacy of the reactor-vessel head design.

2.1.3 VESSEL-TO-SHIELD-POOL CONNECTION

2.1.3.1 Design. Conceptual design of the connection is complete. The arrangement is shown in Figure 2.1. The vessel is connected to the shield pool by a bellows-type expansion joint that permits relative movement between the two which results from differential thermal expansion. The bellows are protected by a sheet-metal ring. A rubber sheet overlapping the ring functions as a leakage limiter in case of bellows failure.

The insulation cover over the flange area, which is removed to obtain access to the flange bolts, is sealed to the vessel and closure by two O-ring seals. Relative motion between the various

Figure 2.1 Shield-pool seal arrangement. (A-C Dwg. 43-401-124)

Figure 2.2 Seal ring gasket test assembly. (A-C Dwg. 43-101-350)

closure penetrations and the insulation shell is also accommodated by bellows-type expansion joints. These joints are protected from physical damage by an assembly that also acts as a leakage limiter.

The liquid level connection is allowed to move within its insulation, thereby precluding the requirement for an air expansion joint.

2.1.3.2 Tests and Studies. Leakage Detection. A study was initiated to find the best method of detecting leakage from the reactor vessel or shield pool into the area above the reactor-vessel cover.

Seal-Ring Gasket Test. The purpose of this test was to determine the operating characteristics of the proposed rubber O-ring seal for the connection between the vessel head insulation cover and the expansion joint assembly. Two characteristics are of interest. First, the load compression curve for various seal durometers and, second, the minimum O-ring compression required to effect a seal against 10-psi water pressure.

The test arrangement is shown in Figure 2.2. The O-ring being tested is placed between two circular flat plates. Load for the load-compression test was applied with a hydraulic jack. Compression is measured with a dial indicator. Water is introduced inside the seal-ring to determine the minimum compression needed to effect a seal.

The load-compression curves for a 9.5-in. i.d. x 1-in dia. O-ring for various durometers are shown in Figure 2.3. The test showed that a compression of 0.025 in. is required to effect a seal. The seal is effective up to 30 psig.

Figure 2.3 Load-compression curves for various O-ring seals.
(A-C Dwg. 43-024-287)

2.1.4 CONTROL-ROD-DRIVE SLEEVE-WELD TESTS

The 2-1/2-in thick vessel head is penetrated by twenty 3-1/2-in dia. schedule 80 Inconel pipes. A full-scale welding test was made to determine the welding characteristics of two possible weld electrodes that could be used for full-penetration welds in the vessel head.

Two mock assemblies were welded using International Nickel, "Inco A" electrode for one assembly and "Inco BP-85" for the other assembly. The compositions of the two electrodes are given in Table 2.1. The weld design of the mock assemblies is shown in Figure 2.4.

Each of the two assemblies consisted of a 2-1/4-in x 12-in x 24-in piece of carbon steel and two 8-in lengths of 4-in dia. Inconel tubing. Assembly 1 was welded with Inco A electrodes and assembly 2 with Inco BP-85 electrodes. The assemblies were not preheated and the interpass temperature was kept below 300 F. After welding the major side, the back side was chipped and welded with the same

TABLE 2.1 COMPOSITION OF ELECTRODES

	Inco "A"	Inco BP-85
Carbon	0.05	0.05
Manganese	1.49	7.5
Phosphorus	0.016	0.020
Sulfur	0.007	0.009
Silicon	0.34	0.51
Nickel	70.18	65.9
Chromium	15.24	15.44
Molybdenum	0.80	0.02
Columbium + Tantalum	1.87	1.99
Iron	9.82	7.7
Titanium	<0.01	0.50
Lead	<0.001	-
Copper	0.17	-

Figure 2.4 Experimental joint design for weld between Inconel tube and carbon steel shell. (A-C Dwg. SI-494)

electrodes used for the major side. The Inco BP-85 had the preferred operability characteristics. In the production weldment, the back-side (inside) of the plate (or vessel shell) will be clad with Type 304L stainless steel. Despite the presence of the 304L cladding, the entire joint should be welded with the Inconel-type electrodes.

After welding, the assemblies were X-ray inspected. The four welds were reported to be satisfactory. Macro-sections were taken and etched as shown in Figure 2.5. for the Inco-A welds and in Figure 2.6 for the Inco-BP-85 welds. The Inco-BP-85 electrodes produced a cleaner weld than did the Inco-A electrodes.

Specimens were cut from the macro-sections for metallographic examination. Photomicrographs were taken of the junctions between the weld and carbon steel and between the weld and the Inconel tubing. (Figures 2.7 and 2.8). There is no observable structural preference of one electrode over the other.

Subsequent time studies made in the A-C shops during fabrication of the full-scale closure for testing revealed the following weld deposit rates using three types of electrodes:

	(in ³ /hr)
Inconel pipe welded with Inco A electrode	0.625
Inconel pipe welded with Inco BP-85 electrode	1.14
Carbon steel pipe welded with 7018 carbon steel electrode	1.75

Figure 2.5 Macrosections of Inconel tubes welded to a carbon steel plate with Inco A electrodes. Actual size.

Figure 2.6 Macrosections of Inconel tubes welded to a carbon steel plate with Inco BP-85 electrodes. Actual size.

Figure 2.7 Microstructures of Inconel tubes welded to carbon steel plate with Inco A electrodes. Left: Weld-carbon-steel junction. Right: Inconel weld junction. Magn: 500 X Etchant: Ferric chloride. (RL Photos IZSZ-11-1, and -2)

Figure 2.8 Microstructures of Inconel tubes welded to carbon-steel plate with Inco BP-85 junction. Left: Weld-carbon-steel junction. Right: Inconel-weld junction. Magn: 500 X Etchant: Ferric chloride. (RL Photo IZSZ 21-1 and -2)

These figures are approximate and include welding, cleaning, and inspection time.

The Inco BP-85 electrode was selected because of its faster deposit rate.

Figure 2.9 Twenty-inch diameter moisture de-entrainment test assembly.
 Items: 1) skirt; 2) housing; 3) strainer; 4) hold-down assembly;
 5) steam separator (see figure 2.10); 6) sight glasses; 7) sampling taps
 (see figure 2.11); 8) orifice plate; and 9) reducing outlet.
 (A-C Dwg. 43-500-764)

2.1.5 REACTOR FEEDWATER RING

A design study was completed for determination of optimum size, number, and location of discharge holes in the reactor feedwater ring. To obtain an even distribution of feedwater around the reactor, the pressure drop through the holes must be large compared to the pressure drop through the ring. On this basis, the final design of 250 holes, 3/8-in dia. was selected. The holes are located in the lower half of the ring, and staggered at angles of 20 and 30 degrees from the vertical. Discharging at these angles, the jets will not impinge on the reactor vessel or internals, and thus eliminate any possibility of erosion or thermal shock.

2.1.6 MOISTURE SEPARATORS

The 20-in dia. test column was fabricated and installed on the Turbine Test Floor at the West Allis Works. A drawing of the test apparatus is shown in Figure 2.9. Saturated steam, which comes from the de-superheater station, enters the bottom of the test column and passes through the diffuser and water column. Natural separation takes place at and above the interface. The steam then passes through the mechanical moisture separator that is being tested and then through an orifice meter and throttling valve.

The first phase of testing includes an evaluation of a mesh type separator and two centrifugal type separators. These separators are shown in Figure 2.10. Steam quality is measured above and below the separator. In order to obtain an accurate steam sample, standard

Fig. 2.10 Steam separators. Top: Mesh-type; Middle and Bottom: Two Centrifugal Type (A-C Photos 207888, 210494, and 210748)

ASME sampling nozzles (Figure 2.11) will be used. A throttling calorimeter will be used to determine steam quality below the separator. The sodium-carryover method, which is more accurate in the 99.9-plus percentage quality range, will be used above the separator. This required the use of a small condenser that is attached to the sampling nozzle. The method of analysis is the same as used in 2- and 4-in dia. tests*. Pressure drop through the separator is measured as a function of steam flow rate and system pressure.

When this phase of testing is complete and the data is analyzed, the type of separator to be used in Pathfinder will be selected.

The mechanical separators will then be removed from the test apparatus and natural separation will be investigated further. Data from this phase of testing will be correlated with data from the 2- and 4-in dia. tests to verify the diametrical correction

* J. Wilson and M. McDermott, "Pathfinder Atomic Power Plant, Final Report, Moisture De-Entrainment Tests in 2- and 4-in Diameter Test Sections," Allis Chalmers Report ACNP-5921.

Figure 2.11 Steam sampling tube and condenser. (A-C Photo 207889)

factor. The same technique described in ACNP-5921 will be used.

Results of this test will also determine the minimum required distance between the inlet of the mechanical separators and the two-phase interface in Pathfinder.

2.1.7 BOILER CORE STRUCTURE

2.1.7.1 Boiler Core Shroud. Conceptual Design. The boiler core shroud separates the downcomer region from the boiler core and supports the upper hold-down assembly. It is composed of a 3/8-inch thick cylindrical shell 79 in. dia. and 89 in. high with end plates attached to each end. The end plates have a center cut out with the same configuration as the core. Figure 2.12 shows a quarter-sized model of the shroud. Fabrication material will be 304L stainless steel.

The shroud is attached to the boiler grid plate with thirteen 1-in dia. bolts and three dowel pins. The fuel element hold-down assembly is attached to the upper end plate.

Figure 2.12 Reactor model (A-C Photo 210441)

2.1.7.2 Tests and Studies. Model of Vessel Internals. A quarter-scale model of the reactor internals is being made. It will be used to check the fit-up of components and to aid in the design of tools and lifting devices for their installation and removal. A photograph of the model is shown in Figure 2.12.

Experimental Stress Analysis. A one-quarter-scale Plexiglas model of the boiler-core shroud which is part of the Model will be used for experimental stress analysis. Fluid friction forces in the core will be transferred to the shroud by the fuel element hold-down structure. The design load is divided between four equally spaced points on the shroud circumference.

The loading on the model will be supplied by a hydraulic jack and measured with a pressure gage. The stresses in the model will be measured by stress-coat analysis and then by strain gages, if necessary. Correlation between the model and prototype will be made by use of similitude.

2.1.8 SUPERHEATER LOWER GRID PLATE

The lower grid plate, which supports the superheater, is 316 stainless steel. The plate will consist of a spherical head segment welded to a flange ring. The spherical portion is perforated by 468 holes with diameters of about 0.80 in.

The spherical grid plate was selected instead of a flat grid plate because the thickness of the flat plate, that would have been

required for the several operating conditions would have resulted in large temperature gradients and excessive thermal stresses.

Abnormal operation conditions that were considered are as follows:

- 1) Simultaneous opening of all four safety-relief valves.
- 2) Opening of the turbine by-pass valve with the turbine throttle valve still open. This could be caused by a control-signal loss.

A pressure differential of up to 200 psig can be expected as a result of these conditions.

Mechanical Stress Calculations. Analysis of stresses due to mechanical loading was done using conventional methods. A deflection efficiency of 50 per cent was used for the perforated plate. This value was based on experimental tests of perforated plates performed at Allis-Chalmers and at other laboratories.

Thermal Gradient Calculations. The spherical grid plate, with the concave side in contact with the moderator fluid at 489 F and the convex side in contact with the superheated steam at 825 F average temperature, was analyzed.

The thermal shield placed on the convex side of the grid plate provides an insulating space of stagnant superheated steam. A similar thermal insulation is provided by superheated steam between the inner and outer container tube.

The film coefficients due to radiation and conduction between the thermal shield and between the inner and outer container tube have been determined. The calculations were based on the assumption of steady-state conditions. The effect of gamma heating was also included.

To determine the thermal gradient across the 3/4-in thick plate, a typical three-dimensional section of the grid plate was chosen. The temperature distribution was determined using the A-C Thermal Analyzer Program* for the 704 IBM computer.

The maximum thermal gradient across a 3/4-in thick plate was found to be 45.5 F, however, due to the curvature of the plate, the thickness near the edge, measured on the vertical plan, is about 1 in; the maximum thermal gradient for this condition increased by 50 per cent or 45.5×1.5 , or 68.5 F.

Assuming the edge of the plate is clamped so that angular deflection but not radial expansion is prevented, the resulting bending stresses may be approximately expressed as follows:

$$S_T = \frac{E \times \alpha \times \Delta T}{2(1-\mu)} \quad (2.1)$$

where

E = Young's modulus of elasticity

α = Coefficient of expansion

ΔT = Maximum temperature difference between the two faces (temperature gradient assumed to be linear)

μ = Poisson's ratio.

The results of the mechanical and thermal stress analysis are presented in Figures 2.13 and 2.14. Stresses at the inner and outer

* A computer program developed by Allis-Chalmers to handle steady-state and transient heat-transfer problems.

Figure 2.13 Stress distribution on inside surface of superheater grid plate. The solid lines denote combined mechanical and thermal stresses. The dotted lines represent mechanical stresses.
 ○= bolted-up condition (Bolt load = 145,000 lb) - $P = 0$ psi
 □= normal operating condition - $P = 85$ psi
 ●= adnormal operating condition - $P = 250$ psi
 (A-C Dwg. 43-024-286)

Figure 2.14 Stress distribution on outside surface of superheater grid plate. The solid lines denote combined mechanical and thermal stresses. The dotted lines represent mechanical stresses.

○ = bolted-up condition (bolt load = 145,000 lb) - $P = 0$ psi

◻ = normal operating condition - $P \approx 85$ psi

● = abnormal operating condition - $P \approx 250$ psi

(A-C Dwg. 43-024-285)

surfaces of the spherical shell are given by Figures 2.13 and 2.14, respectively. Dotted lines represent mechanical stresses.

Solid lines represent combined mechanical and thermal stresses. The apparent discontinuities in the stress distributions shown are the

result of a stress concentration factor applied because of the presence of the perforations. In the analysis, the following sets of conditions were assumed:

- 1) Bolted-down condition, no pressure or temperature differential.
- 2) Normal operating condition (85 psi pressure differential).
- 3) Adnormal condition (250 psi pressure differential).

The design seems adequate in all cases.

2.2 RECIRCULATION PUMPS

The objectives of this project are as follows: 1) to develop reliable, efficient recirculation pumps that will require a minimum of maintenance; and 2) to develop an efficient shaft seal.

2.2.1 PROTOTYPE RECIRCULATION PUMP

The manufacturing of the prototype recirculation pump is about 90 per cent complete. The pump casing was finish machined and the suction elbow was welded on. The impeller casting was behind schedule about a month because of a machining error. A curved section on the impeller shroud was machined too thin resulting in uneven cooling and distortion of the shroud after heat was applied for the stellite processes. The distortion was satisfactorily corrected.

The prototype pump-motor unit is being assembled. The cold hydraulic test will be run during July to determine pump characteristics.

The test of the prototype floating-bushing shaft seal was completed by the Allis-Chalmers Centrifugal Pump Department. Over

200 hours of testing was completed covering all pressures in the Pathfinder operating range. Favorable preliminary results are reported by the Centrifugal Pump Department in regard to leakage, pressure drop and seal injection water requirements.

Materials for the prototype trunnion mounting have been ordered. A mechanical test of the trunnion mounting structure will be carried out in the hot test loop, with the prototype pump running at operating pressure and temperature. A mechanical test specification is being written. Information will be obtained on (a) the frequency and amplitude of pump and loop vibration, (b) movements of the pump caused by thermal expansion of the pump and test-loop piping, and (c) friction forces in the sliding plate of the mounting. During the test, the end of the hot test loop will be rigidly anchored to simulate Pathfinder recirculation system conditions.

2.2.2 RECIRCULATION LOOP VALVES

The specifications for the butterfly valves and operators were written. The valve operators are of the electric-motor gear-drive type that fail in position on loss of operator power. The operators for the pump discharge valves, which are used to control flow, were chosen so that the total rate of change of flow rate will not exceed 455 gpm/sec, which corresponds to an external reactivity addition of about 5 cents/sec. The discharge valve operators are of the two-speed type with a faster speed from the closed (0°) to 35° open position and a slower speed in the range of 35° open to full open. This permits the valve to traverse the cavitation region faster. An interlock

prevents starting of the recirculation pump unless its discharge valve is 50° open and will ensure a gradual increase in flow upon starting. Also, the discharge valve automatically opens to 40° when a pump is started so that operating time in the cavitation region is minimized. Interlocks with the discharge valves prevent a second or third pump from starting when any discharge valve is

opening, so that the rate of flow addition does not exceed

455 gpm/sec. Provision for ganged operation of the three discharge valves in the control range (40 to 90°) is being provided to give faster flow control. Calculations show that the 455 gpm/sec rate of flow addition is not exceeded upon start-up of a pump or during ganged operation of the discharge valves in the control range.

The fastest method for coming up to full flow using the specified valve operators is shown in Figure 1.15. When a pump is started, its discharge valve opens at the faster speed to 35° and continues to open to 40° at the slower speed, thus producing the change in slope noted in the curve. Upon start-up of the first pump, its discharge

Fig. 2.15 Recirculation flow rate increase as pumps are started up. (A-C Dwg. 43-024-288).

valve automatically opens to 40° , which corresponds to a flow of about 21,000 gpm. Starting the second pump at this point causes its discharge valve to open to 40° , thereby increasing the flow to about 38,000 gpm. Starting the third pump causes its discharge valve to open to 40° increasing the flow to about 50,000 gpm. Finally, the three discharge valves are ganged at 40° and brought to the full-open position, which increases the flow to about 62,700 gpm.

The range of flow control available during three pump operation is from 62,700 gpm (60,000 gpm nominal value) to 45,000 gpm.

2.3 STEAM SEPARATORS

The objectives of this project are as follows: 1) to develop an efficient, reliable system for the separation of steam from recirculating water; and 2) to prove by means of full-scale tests that this system will be workable.

Several tests were run on the full-sized steam separator model to determine the effect of bubble size on separator efficiency. The first test was carried out by testing the separator with 10-mesh screen, 6-mesh screen, and without a screen over the baffle sleeve outlet. The percentage of air carried into the separator and the percentage of air carried under with the water were measured.

The screens were fastened over the baffle sleeve outlet as shown in Figure 2.16.

Figure 2.17 shows a plot of the percentage of air carried into the separator for the three conditions as the recirculation flow

rate was varied. These tests were made with a static water level of 40 ft above the baffle sleeve outlet. It was expected that the curves would show a progressive change in percentage of air into the separator as screen size became smaller. It can be seen from Figure 2.17 that no progressive change took place. The curve for no-screen falls between the curves for 10- and 6-mesh screens.

Percentage of air carried under with recirculating water as recirculation flow rate was varied is shown in Figure 2.18, for the three cases. These curves also indicate no progressive change. It appeared, therefore, that some condition other than the presence of screens was making the data erratic.

Upon observing the flow configuration in the standpipe, the possibility that turbulence in the test section was causing an effect on the separator was considered. The circular shape of the standpipe promotes the formation of vortices in the downcomer area. This action could cause more air to be carried into the separator

Figure 2.16 Cutaway view of steam separator test loop. (A-C Dwg. 43-002-199)

Figure 2.17 Amount of air carried into the separator with screens over the baffle sleeve outlet. (A-C Dwg. 43-024-234)

Figure 2.18 Amount of air carried under with and without screens over the baffle sleeve outlet. (A-C Dwg. 43-024-235)

than would be expected in the reactor where straight-line flow is presumed. The turbulence would also account for the unpredictable positions of the curves in Figure 2.17 and 2.18.

A second series of tests were made with two different flow straightening devices added to the test loop. These devices were installed in the downcomer area around the separator, as shown in Figure 2.16. The first of these was a single vane and the second was a multi-vane assembly. After the vanes were tested, 16-mesh screen was placed over the baffle sleeve outlet to determine its effect.

The percentage of air carried into the separator for the three cases when the single-vane and multi-vane devices and no vane were used and as the recirculation flow rate was varied. The curve shows that the percentage of total air into the separator at reactor recirculation flow rate for one separator (1330 gpm) decreased from 45 per cent to about 20 per cent when the vanes are added. It is considered judgment that the data obtained with the vanes in place are closest to what may be expected in the reactor.

Several test runs were made with 16-mesh screen over the baffle sleeve outlet. The multi-vane flow straightening device was used in these runs. Results of this test are also shown in Figure 2.19 and indicate that the percentage of air going into the separator is a more linear function of recirculation rate when the screen is in place. A possible explanation of this is that, in general, smaller sized bubbles were present with the screen in place. Smaller bubbles

Figure 2.19 Amount of air carried into separator with vanes in test loop downcomer. (A-C Dwg. 43-024-233).

Figure 2.20 Amount of air carried-under with vanes in test loop downcomer. (A-C Dwg. 43-024-231)

Figure 2.21 Amount of air carried under in steam separator test loop without a separator as air flow rate and recirculation flow rate vary. (A-C Dwg. 43-024-230)

Figure 2.22 Pressure drop through the separator at various air flow and recirculation flow rates. (A-C Dwg. 43-024-232)

are less buoyant and follow the lines of water flow more closely. With no screen in place, bubbles were probably larger and required a higher downcomer velocity to carry them into the separator.

Figure 2.20 shows curves for percentage of air carried under for the same cases considered in Figure 2.19. Air carried under with water remains within 0.05 per cent over all but the highest flow rates, even though the amount of air into the separator varies.

To determine the efficiency of the separator, tests in the loop without the separator were made. Air and water flow rates were the same as during tests with the separator. Results are shown in Figure 2.21. At rated flow, carry-under was as high as 4.7 per cent. With the separator in place, carry-under was only about 0.1 per cent. Figure 2.21 also shows that carry-under starts at about 400 gpm or 0.66 fps water velocity in the downcomer for all air supply rates.

A plot of total pressure drop through the steam separator as a function of recirculation flow rate is shown in Figure 2.22. At rated conditions, pressure drop through the separator model was 4.4 ft of water.

No further tests are planned. Results have shown that the separator is highly efficient in performing its function of removing entrained steam from the recirculating water. This permits operation of the recirculation loops at rated flow and head without damage due to cavitation. Results of the entire program will be prepared for issuance as a final report. This project will no longer be reported in this quarterly report.

2.4 CONTROL RODS, GUIDE TUBES, AND CONTROL ROD DRIVES

The objective of this project is to perform investigations and experimental work in regard to the control rods, control rod guide tubes, and control rod drives leading to fabrication of effective and economical components for the Pathfinder reactor.

2.4.1 CONTROL RODS

Design of the test apparatus to test superheater control rods has been completed. The test apparatus is now being fabricated in the shop.

2.4.2 GUIDE TUBES

The Zircaloy-2 guide tube for the prototype hot tests has been received, and has been placed in the test set-up.

2.4.3 CONTROL ROD DRIVES

2.4.3.1 Prototype Control Rod Drive. The prototype control rod drive is assembled and will be tested during the next quarter. The drive was assembled and cleaned in a special room at West Allis Works according to specifications covering the cleaning of reactor components that come in contact with primary coolant. Some subassemblies are shown in Figures 2.23 and 2.24.

In addition, an oil accumulator to pressurize the motor housing and a carbon rack backup bearing to fit between the outer containment housing and pinion housing are being built. The bearing is needed to prevent the rack from bouncing on the round Stellite rack roller at the bottom of the stroke where the teeth are cut completely around the rack. A drawing of the complete assembly is shown in Figure 2.25.

Figure 2.23 Drive housing assembly. (A-C Photo 210401C)

Figure 2.24 Rack and housing assembly (A-C Photo 210401D)

Figure 2.25 Control rod drive layout. (A-C Dwg. 43-002-186)

TABLE 2.2. COMPARISON OF CHARACTERISTICS OF
SINGLE-AND THREE-PHASE MOTORS.

	SINGLE-PHASE MOTOR		THREE-PHASE MOTOR	
	NO LOAD	240 in-lb. LOAD	NO LOAD	240 in-lb. LOAD
Starting Time - up (Milliseconds) - down	90-100 90	140-150 80-90	140-150 140	240-280 130-140
Stopping Time - up (Milliseconds) - down	80-95 95	50 75-90	150 150	170 230
Reversing Time - up to down (Milliseconds) - down to up	155 190	115 250-260	145-165 150	170 230
Motor RPM - up - down	1710 1800	1500 1800	1640 In Air: 1650 1760 1770	1240-1265 1770
Output RPM - up (105.8:1) - down	16.2 17.0	14.2 17.0	15.5 15.6	12.0 16.6
Power - up (Watts) - down	250 250	270 240	95 90	170 70
Line Current - up (Amperes) - down	2.87 2.82	2.68 2.92	1.13 1.12	1.3 1.15

2.4.3.2 Tests. Control Rod Drive Hot Tests. The pressure vessel for the drive test was assembled, insulated, and hydrostatically tested. Wiring for instrumentation is nearly complete. The hot test program will permit testing the following items under reactor temperature and pressure:

- Operating life of the drive
- Drive motor and clutch characteristics
- Heat transfer to the shield pool
- Seal leakage
- Vibration or instability
- Scram time
- Dashpot operation
- Position indication

Control Rod Drive Motor Test. A three-phase gear motor was selected as the drive motor for the prototype as a result of tests conducted in Greendale Laboratories. The test results indicated that a single-phase motor did not register sufficient difference between load and no-load current or power to indicate whether the control rod was latched to the control rod drive. Characteristics of the motors are shown in Table 2.2.

Control Rod Drive Housing Hydrostatic Test. The prototype pinion housing was hydrostatically tested to 1300 psig and showed no inelastic deformations. This test was necessary because the stresses in the many welds and joints in the housing would have been very difficult to analytically determine.

Figure 2.26 Operating characteristics of magnetic clutch.
(A-C Dwg. 43-024-282)

Magnetic Clutch Tests. Tests were conducted to determine the holding action of two alternate clutches used for connecting the drive motor to the control rod pinion. The clutches were of similar design with different size and number of disks. The model SC 375 was selected for the prototype because of its physical size and lower inertia, although it is operating very close to its rated torque. Figure 2.26 shows the torque-current relationship of the clutches.

3. NUCLEAR ANALYSIS

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3.1 REACTOR PHYSICS (STATICS)

The objectives of this project are as follows: 1) to evaluate the characteristics of the reference core and optimize the design to achieve safe, reliable, and economical performance; and 2) to optimize shielding designs so as to arrive at an adequate and economical design.

3.1.1 FLUX DISTRIBUTIONS

Detailed flux distributions in an X-Y plane at the axial position of maximum superheater fuel temperature have been obtained for various control-rod insertions. These distributions are shown in Figure 3.1 where no control rods are inserted, Figure 3.2 with only the superheater control rods inserted, and Figure 3.3 with four boiler control rods inserted and superheater control rods withdrawn.

The power distribution in the superheater remains relatively flat for the cases with no rods inserted and with just boiler control rods inserted. A summary of results is given in Table 3.1

TABLE 3.1 SUPERHEATER POWER GENERATION CHARACTERISTICS WITH CONTROL RODS FULLY INSERTED*

Rods Inserted	Superheater Power Fraction	Max-to-Ave Superheater Power
No rods Inserted	0.1685	1.055
Sptr rods Inserted	0.1149	1.518
Four boiler rods inserted	0.1822	1.097

* All results are for initial core load.

3.1.2 LOCAL FLUX PEAKING

The boiler fuel rods are made up of 18-in long fuel pins. The joints between the pins with the fission gas spaces and the transition region between the 81- and 64-pin fuel bundles produce three axial positions within the core that are void of fuel. The thermal flux

Figure 3.1 Relative power distribution (X-Y plane about 2 ft from bottom of core) at nominal hot operating core condition. The control rods and channels are represented explicitly. All control rods are withdrawn. (A-C Dwg. 43-401-145)

Figure 3.2 Relative power distribution (X-Y plane approximately 2 ft above bottom of core) at nominal hot operating core condition with superheater control rods inserted. Control rods and channels are represented explicitly. (A-C Dwg. 43-401-146)

Figure 3.3 Relative power distribution (X-Y plane approximately 2 ft above bottom of core) at nominal hot operating core condition. Control rods and channels are represented explicitly. Boiler control rods cross-hatched are inserted. (A-C Dwg. 43-401-147)

peaking at these positions has been calculated with the Flip-1 program using the double P-3 approximation.

Three methods of reducing power peaking were studied:

- 1) reducing the enrichment of those fuel pellets near the gap, 2) the placement of a high cross-section poison at the edge of the gap, and
- 3) a combination of the two.

Figure 3.4 and Table 3.2 show a comparison of the effectiveness of the methods at the center position of the 81-pin fuel bundle. This position

is used for the comparison since it is the position of maximum flux peaking. The transition region would normally represent a higher flux peak in that it occurs at the maximum axial flux but because the gap at that position is smaller, the overall flux peaking is less.

Curves B and A on Figure 3.4 represent thermal flux peaking in the gap with 1/2- and 1-in lengths of natural uranium located adjacent to it. Curve E represents flux peaking with high cross-section poison wafers adjacent to the gap. Curve D represents flux peaking with a low cross-section poison (disprosium oxide) inserted in a 1/2-in long pellet of natural uranium adjacent to the gap. Curve C is the reference flux.

TABLE 3.2 POWER PEAKING DUE TO FUEL GAP IN 81-PIN REGION

Power Source	Max-to-Ave Power
Reference Case	1.803
1/2-in Natural UO ₂ Fuel	1.692
1-in Natural UO ₂ Fuel	1.599
High Cross-section poison wafer	1.128
1/2-in Natural UO ₂ with low cross-section poison	1.230

Figure 3.4 Flux peaking at gap in 81-pin region with 10 per cent voids. (A-C Dwg. 43-024-307)

The reduction in the hot spot factor for the first two cases is inadequate. With high cross-section poison wafers and with the combined poison and natural uranium pellet adjacent to the gap, the hot spot factor is reduced significantly.

Since both poison and fuel will burn-up with time, resulting in changes of the spatial distributions of both the flux and the fission cross section, the hot spot factor was calculated as a function of irradiation using the isotopic changes of U-235, Pu-239, Pu-240,

Pu-241 and the poison in time. A new distribution was generated at the end of each time interval and held constant over the next interval. The total poison used in these two cases was selected on the basis of the amount desired as a burnable poison for reactivity compensation. Use of presently developed high cross-section burnable poison materials such as boron results in a mismatch in its depletion rate relative to that of the fuel. This causes a significant power peaking with irradiation. A disadvantage of a low cross-section poison in a natural uranium pellet is the residue remaining at the end of the irradiation cycle. However, the hot spot reduction can be maintained throughout core lifetime. The temporal behavior of one case is shown in Table 3.3. Burnout effect of the uncompensated case is also shown.

Further analysis is being made with other combinations of the amount of poison and the enrichment of the compensating pellet.

TABLE 3.3 POWER PEAKING DUE TO FUEL GAP IN 81-PIN REGION WITH POISON AND NATURAL UO_2 COMPENSATION AS A FUNCTION OF IRRADIATION

Time (Mo.)	Maximum-to-Average Reference Power	Residual Effective Poison (percent)	Maximum-to-Average Compensated Power
0	1.803	100.0	1.230
2	1.794	92.6	1.285
4	1.774	78.8	1.343
6	1.745	60.8	1.394
9	1.678	46.1	1.407
12	1.613	34.3	1.390
15	1.541	25.0	1.390

3.1.3 CORE BURNUP

The reactivity loss with fuel depletion was determined by holding the initial flux and power distribution constant throughout the core lifetime. A series of two-dimensional calculations were then made to determine initial burnable poison requirements. The requirements were formulated so as to maintain the total reactivity swing within specified limits and to maintain a uniform power fraction between the superheater and boiler regions throughout the core lifetime.

The equivalent homogenized thermal absorption necessary to hold down the excess reactivity present in the hot operating core at xenon equilibrium was calculated using the weighting factors that had been used in the evaluation of the reactivity loss. After obtaining the equivalent poison requirements, a series of one dimensional radial calculations were made through the 81- and 64-pin regions with various ratios of homogenized poison present in the boiler and superheater. Some of the results of these calculations are given in Table 3.4. The calculations indicated the amount of reactivity to be held down by control rods and the amount of reactivity to be contained in burnable poison, and the location of the poison required to obtain the correct power ratio between the superheater and boiler.

After the initial poison requirements were thus specified, the core with a full complement of poison was evaluated with the Candle-II code to determine the effectiveness of the poison through core lifetime. Of particular interest was any shift in power between the

superheater and boiler. Because of the more rapid fuel depletion in the highly enriched superheater region, both the amount and the effective microscopic cross section of superheater poison were varied to determine the optimum type and amount that should be used.

Results of some of the Candle-II calculations through the 81-pin section are shown in Figure 3.5. The curves represent two separate calculations for two types of superheater poison. Curve B represents a case where boron is used as poison in both the superheater and boiler. Curve A represents the case for the same poison distribution but with a poison of higher microscopic cross section in the superheater. The dotted curves represent the amount of excess reac-

TABLE 3.4 POWER FRACTION OF THE SUPERHEATER WITH DIFFERENT BURNABLE POISON COMBINATIONS

Core Region	Macroscopic poison cross-section			Superheater Power Fraction
	Boiler	Superheater	K	
81-Pin	0.00600	0.02200	1.004	0.177
64-Pin	0.00600	0.02200	0.978	0.222
81-Pin	0.00700	0.02200	0.993	0.185
64-Pin	0.00700	0.02200	0.966	0.233
81-Pin	0.00600	0.02000	1.008	0.188
64-Pin	0.00600	0.02000	0.983	0.236
81-Pin	0.00500	0.02000	1.019	0.180
64-Pin	0.00500	0.02000	0.996	0.224

Figure 3.5 Reactivity held by control rods and burnable poison through core lifetime. (A-C Dwg. 43-024-306)

tivity that is held down by control rods. This curve was obtained by using a poison search for criticality in the Candle-II code. Table 3.5 shows the power shift into the superheater that is obtained with burnup. The power shift remains quite small when boron is used as poison for both regions.

3.1.4 ZIRCONIUM CORE COEFFICIENTS

A study of the reactivity changes in Pathfinder resulting from heating and void formation in the boiler, and heating and flooding in the superheater was completed. The results are given in Table 3.6.

TABLE 3.6 FULL CORE GROSS REACTIVITY COEFFICIENTS

Boiler Condition	Superheater Condition	Reactivity	Remarks
Cold	Voided	1.0438	The cold-to-hot penalty is $-.021$ including the Doppler effect of $.014$. Reactivity in voids is $-.023$. The void coefficient is $-.00022$ per percent of operating voids. Cold flooding coefficient is $-.0073$. Hot flooding coefficient is $-.0057$. Operating flooding coefficient is $.0049$.
Hot, nonboiling	Voided	1.0227	
Hot, operating voids	Voided	.99959	
Hot, 95 per cent of operating voids	Voided	1.00081	
Cold	Flooded	1.0365	Operating flooding coefficient is $.0049$.
Hot, nonboiling	Flooded	1.0170	
Hot, operating voids	Flooded	.9947	

In this study, a two-dimensional (R-Z) cylindricized model of the core was used. The superheater was represented by 8 regions, the baffle between the boiler and superheater by 2, and the boiler by 22. A steam void content was assigned to each of the boiler subdivisions.

Nuclear constants of the boiler were obtained by the technique described in Section 3.1.4 of ACNP 6006. The necessary weighting data were obtained with the Flip-1 program using the double P-3 approximation. Constants were prepared for the unvoided, cold condition and for a range of steam void content at operating temperature. It is assumed throughout that the water between the boxes and in the control rod gaps does not void.

Neither the control rod gaps nor the fission gas spaces were included explicitly in the R-Z representation of the core. Accordingly, the boiler constants referred to above, which were calculated for a uniform array of fuel boxes separated by .138-in of water, had to be modified by two homogenization steps. The first step was to average the basic nuclear constants with the control rod gaps. This gave radially homogeneous numbers. Volume-averaging was used for fast constants and volume-plus-transport derived fluxes were used for averaging the thermal constants. A I/V poison absorption of 0.004 cm^{-1} at operation was added to the radially averaged results to achieve a reasonable superheater power fraction in the final calculations.

The second step was to average the radially homogeneous values with the fission gas spaces. Again, volume weighting was used for the fast group but volume-plus-diffusion calculated flux weighting was used for the thermal group.

The fission gas space data that was used in the final averaging is based upon the assumptions that first the gaps are 2.27 in. wide at the axial midpoint of the upper and lower core halves, and 1.42 in. wide at the core center. Second, the cross-sections used to describe these regions correspond to a 40 per cent replacement of the removed UO_2 with zirconium in the core halves, and full replacement with zirconium in the center. No poison, which may be incorporated later into the gap structure to control power peaking, was included.

Constants applying to a fully homogenized model of the superheater have been developed. Cold voided and flooded, and hot voided and flooded conditions were represented by the set of values used in this study.

A stainless steel baffle 0.25 in. thick is located between the boiler and superheater sections of the core. It has been calculated as if it were solid steel in the bottom half of the core, and half steel and half water in the upper half. Grey slab theory* was used to get the thermal group constants where the steel is solid, and ordinary diffusion parameters were used for the metal-water mixture.

Generally, the calculated reactivity penalties are acceptably small. The incremental void case indicates that the derivative of reactivity with void content over the operating range is essentially constant.

3.1.5 SHIELDING

The primary concrete shield around the reactor vessel was rechecked to insure that ten feet of ordinary concrete is adequate for shielding purposes. The dose on the horizontal centerline of the core outside the shield is 1 mr/hr. The total heat load due to gamma heating is still 15 kw with an uncertainty of plus 15 and minus 10 kw.

The fuel storage pool was re-evaluated assuming the racks are loaded with the following three quantities of spent fuel: 1) one-half boiler core plus one superheater core; 2) one boiler core plus two

* S. Stein, WAPD-149

superheater cores; and 3) two boiler cores plus three superheater cores. The fuel that has decayed the longest is assumed to be in the rack nearest the pool wall.

The dose rates outside the pool wall on the mezzanine floor are 5, 6, and 95 mr/hr for the three cases, respectively. These dose rates include a factor of three of uncertainty. The maximum gamma heat load in the pool wall was calculated to be 1.7, 2, and 32 Btu/hr-ft³ for the three cases, respectively. (The dose for the third case is much less (5 to 10 mr/hr) on the basement floor.)

The N-16 distribution in the turbine building was re-evaluated using the latest information available on source size and steam flow rates. The strength of the volume sources is given in Table 3.7. The fraction of N-16 produced in atoms/sec in the steam was also calculated and is plotted as a function of reactor power in Figure 3.6.

TABLE 3.7 STRENGTH OF N-16 VOLUME SOURCES

Item	Sv (Y/cm ² -sec)
Main Steam Line	2.36×10^4
HP Turbine*	2.36×10^4
Cross-over pipes (2)	5×10^3
LP Turbine	
a) mainsection**	4.7×10^3
b) exhaust flanges (2)	9.6×10^1
Condenser	
a) transition volume	9.6×10^1
b) main volume	2.1×10^2
c) air baffles	5.4×10^3
Feedwater heaters	
a) 1	3×10^4
b) 2	3×10^4
c) 3	4.6×10^4
d) 4	4.1×10^4
Air ejector	
a) inter-cooler	4.3×10^5
b) after-cooler	2.4×10^6

* Varies exponentially along the axis from the front to the back from 2.36×10^4 to 5×10^3 , respectively.

** Varies exponentially along the axis from the center towards each end from 4.7×10^3 to 9.6×10^1 , respectively.

Figure 3.6 Fraction of N-16 production (atoms/sec) vs per cent reactor power. (A-C Dwg. 43-024-248)

TABLE 3.8 GASEOUS FISSION PRODUCT PRODUCTION PER FUEL PIN (1.4 year irradiation time)

Isotope	Molecules
Br	2.63×10^{19}
Kr	1.8×10^{21}
I	6×10^{19}
Xe	6.9×10^{21}
Total	8.8×10^{21}

The gaseous fission product production per fuel pin was calculated.

A 1.4 year irradiation time was assumed for the calculations. The total gaseous fission product production in molecules is given in Table 3.8.

The activity of the closure and the control rod latches due to the deposition of crud and the (n, γ) reaction in the material itself was calculated. It was assumed that the crud reaches maximum activity before it is deposited on the closure and control rod latch. A uniform 5-mil thick plate-out of crud over the exposed surfaces is assumed.

The doses from the closure immediately after shutdown and after one-day decay are given in Table 3.9. The dose at the surface of the control rod latch immediately after shutdown are 890 mr/hr and 50 R/hr for the latch and crud, respectively, for a total of 50 R/hr; after two month decay, the dose is 200 mr/hr and 50 R/hr for the latch and crud, respectively, for a total of 50 R/hr. If the crud can be re-

moved from the items, the doses are fairly low and maintenance could be performed on these items for short periods of time after a suitable decay period.

Figures 3.7, 3.8, and 3.9 show the various areas of radiation and their levels. All areas that receive doses greater than 30 mr/hr are restricted areas during full power operation. All dose levels include a factor of three of uncertainty and the maximum dose is used in describing the various areas.

In the turbine building, the primary source of radiation is the N-16 carried by the steam. The levels given for this building include the contribution of all equipment that carries or uses this steam.

In the fuel handling building, the primary source of radiation is the crud carried by the water. The dose levels include contributions from all sources of activity. The active isotopes in the crud are given in Table 3.10.

DOSE ONE INCH FROM CLOSURE
TABLE 3.9

Item	Top of Closure		Bottom of Closure	
	At Shutdown (mr/hr)	After 1-day decay (mr/hr)	At Shutdown (mr/hr)	After 1-day decay (mr/hr)
carbon steel	4750	10	950	2
stainless steel	7	7	100	100
crud	1420	1420	22000	22000
Total	6177	1437	23050	22102

(Continued on page 135)

Figure 3.7 Pathfinder plant dose rates, operating floor
(A-C Dwg. 43-500-838)

Figure 3.8 Pathfinder plant dose rates, mezzanine floor
(A-C Dwg. 43-500-836)

Figure 3.9 Pathfinder plant dose rates, basement floor
(A-C Dwg. 43-500-837)

TABLE 3.10 ACTIVE ISOTOPES IN CRUD

Isotope	γ Energy	Half-life
Fe-59	1.0	46 days
Co-60	1.0	5.28 yr.
Mn-56	1.0 and 2.0	2.59 hr.
Cr-51	0.3	26 days
Zr-95	0.75	65 days
Zr-97	0.7 and 1.40	17 hr.
Ni-65	1.5	2.56 hr.

In the reactor building the main source of radiation is primary and secondary gammas from the core. Near the recirculation pumps, plate-out of crud and N-16 in the water were also considered as sources of activity.

The pump-room dose rates, which are not shown on the drawings,

are 0.5 to 30.0 R/hr. The dose rates in the turbine building near the main steam line and feedwater heaters are 500 to 1000 mr/hr.

3.2 REACTOR AND SYSTEM DYNAMIC ANALYSIS

The objectives of this project are as follows: 1) to determine analytically the dynamic behavior of the reactor and related systems under normal and abnormal conditions, and 2) to determine the effectiveness of the reactor control systems and the nuclear instrumentation requirements.

3.2.1 REACTOR RESPONSE TO RECIRCULATION FLOW DISTURBANCES

Recent studies have included approximating calculations of recirculation flow rate after simultaneous power loss to one, two, and three pumps. The flow coasts down to 73, 37, and 12 per cent, respectively, assuming 1) that the control rods are not scrammed and 2) that the butterfly valve positions are not changed during coast-down. In each case, the approximations indicate the recirculation flow will decay smoothly to its final value in less than one-half minute.

Figure 3.10 Twenty-second coastdown (to 38 per cent) of all re-circulation pumps due to voltage loss and self restart on 1-sec time constant --- P_2 regulation. (A-C Dwg. 43-024-185)

Results of these calculations were used in an analog study of the case where power to all three pumps is simultaneously lost and full-line voltage is again restored to the pump motors after flow coastdown. Results are shown in Figure 3.10. The final power level of approximately 20 per cent is reached after about 20 seconds. After line voltage is restored, the recirculation flow is driven to its rated value in about 5 seconds, if the butterfly valves are left wide open.

A second solution for loss of one recirculation pump is shown in Figure 3.11. In this case, the pump was restarted after its controlling butterfly valve was driven to a near-closed (5°) position. The pump was then started and the valve was opened manually by controls that limit the rate of flow change to 455 gpm/sec. In this case, the reactor power peaked at about 115 per cent of full power.

The results thus show that it will be inadmissible to start a pump at any time with its discharge valve wide open. Present operating procedures, which include interlocks for the pumps and controlling butterfly valves, will therefore be limited as follows: 1) before any pump can be started, its discharge butterfly valve must be almost closed (5° or less from its seat); and 2) the maximum rate of flow rate change during any mode of operation (singly or ganged) will be 455 gpm/sec.

Figure 3.11 Momentary loss of one recirculation pump with P_2 regulation. Dotted curve represents self restart on a 1-sec time constant. Solid curve represents controlled manual restart.
(A-C Dwg. 43-024-183)

3.2.2 EFFECT OF VOID COEFFICIENT ON TRANSFER FUNCTION

The effect of different values of void coefficient on the closed loop transfer function was investigated on the simulator by varying the coefficient (based on exit voids) from 1.25×10^{-2} to 5.0×10^{-2} per cent δk /percent V_{exit} .

Figure 3.12 shows that as the void coefficient increases from the low value, the high-frequency resonant peak becomes higher and sharper; and the resonant frequency increases slightly. The result is

Figure 3.12 Effect of void coefficient of reactivity on closed loop transfer function high-frequency peak. Reference (accepted) void coefficient is 2.5×10^{-2} . Curve A represents the function with a void coefficient of 1.25×10^{-2} ; and curve B represents the function with a void coefficient of 5.0×10^{-2} . (A-C Dwg. 43-024-240)

expected since greater reactivity feedback is associated with the greater void coefficient (Curve B). Thus, this is equivalent to operating the reactor at a higher void content, which is nearer the threshold of sustained oscillations. Preliminary calculations have shown that with the reference void coefficient of 2.5×10^{-2} per cent δk /percent V_{exit} , the threshold of sustained oscillations will be encountered at 480 per cent of the design power.

3.2.3 AUTOMATIC CONTROL OF REACTOR POWER

Automatic control of reactor power was simulated by two different methods. First, a signal proportional to the difference between plant electrical output and electrical demand was sent to the recirculation control valves. An increase in electrical demand would in-

Figure 3.13 Automatic reactor power control through automatic control of recirculation flow for a 2-percent power increase demand (0.1 percent/sec for 20 sec) with P_2 control. (A-C Dwg. 43-024-178)

Figure 3.14 Automatic reactor power control with automatic continuous control of control rods for a 2-percent power increase demand (0.1 per cent/sec for 20 sec) with P_2 regulation. (A-C Dwg. 43-024-179)

crease recirculation flow rate and consequently reactor power. In a second method of automatic control, the same difference signal was sent to the control rod actuators, which provided a continuous adjustment. An increase of electrical demand would automatically raise the rods to increase reactor power.

The solution to both problems is given in Figure 3.13 and 3.14. In both cases, steam-line pressure control of the turbine inlet valves was used.

3.2.4 SIMULATOR

Several changes were introduced into the simulator this quarter. A new derivation for superheater temperatures made the describing equations less complicated by working with steam enthalpies rather than steam temperatures. An example of changes is shown in the equation that describes the outlet steam temperature variation:

New Equation

$$\dot{Q}_{sh} = (h_2 - h_1)W_s + \frac{M_s \bar{C}_p}{K} \frac{dT_2}{dt}$$

Old Equation

$$\dot{Q}_{sh} = \bar{C}_v K (T_2 - T_1) W_s + M_s \bar{C}_v \frac{dT_s}{dt}$$

$$T_s = C(T_1 + T_2)$$

where,

- $Q_{sh}(t)$ = Heat absorbed by steam in superheater.
- $W_s(t)$ = Steam flow rate.
- M_s = Mass of steam in superheater.
- $C_p(T)$ = Specific heat at const. pressure.
- $C_v(T)$ = Specific heat at const. volume.
- K = $\bar{C}_p / \bar{C}_v$
- $T_1(t)$ = Inlet steam temperature.
- $T_2(t)$ = Outlet steam temperature of superheater.
- $T_s(t)$ = Average steam temperature in superheater channel.
- C = Constant

(t) indicates a quantity is a function of time.

(T) indicates a quantity is a function of temperature.

Another change was a re-derivation of the subcooling in the boiler region. This derivation now accounts for the presence of purification water and recirculation pump seal water.

Also, the entire simulator was re-scaled so that large deviations in all parameters can be studied. This change will allow investigations of cold water, isolation, and start-up accidents.

An interim report (ACNP-6005) covering all simulator results to April 1, 1960 was published.

3.2.5 TIME DEPENDENT NEUTRON-FLUX DISTRIBUTION

An analytical investigation produced a simple feedback model that was successful in predicting the stability threshold for a point reactor. This same feedback model will now be applied to the spatial problem to detect any flux tilting tendency due to void formation.

An analog computer solution for a core with Pathfinder dimensions and Pathfinder material parameters showed no tendency to flux tilting due to boiling disturbances. This was checked for radial and axial models, assuming that these directional effects can be separated. An example of the time varying flux shape for the radial model is shown in Figure 3.15. This figure shows that the flux shape remains essentially constant during the transient and did not have a tendency to tilt. More detailed investigations of disturbances with these models will be continued.

Since there is inadequate knowledge about any flux tilts and their causes in a boiling-water reactor, it was decided to provide places for nine in-core ion chambers in superheater fuel tube positions

Figure 3.15 Variation of radial flux distribution after a sudden pulse of neutrons at core center. Analog computer solution. Times indicated are after disturbance. (A-C Dwg. 43-024-241)

in Pathfinder. This provision was made despite the indications of the preliminary study mentioned above. These chambers will be placed in three radial positions on a diameter (core center and outer edges of the superheater); axially, they will be placed at the positions of the second harmonic loops. This was done since the second harmonic of the flux distribution would probably become excited the most during a flux tilt.

3.2.6 NUCLEAR INSTRUMENTATION

The Bendix Corp. was chosen as nuclear instrumentation supplier. Their system consists of three BF_3 startup channels, one log-N compensated ion chamber channel (CIC), two safety channels with uncompensated ion channels, and two operating channels (CIC). Associated with these channels is an automatic shutdown system that includes two-out-of-four coincidence scram circuits and necessary logic elements. The entire system is of solid-state construction.

3.3. CRITICAL EXPERIMENTS

The object of this project is to perform the necessary critical experiments to check the theoretical calculations and provide a means of correlating theoretical and experimental results.

The critical experiments on slab geometries of boiler core fuel, which constitutes phase I of the critical experiments for Pathfinder, were reported in the last technical progress report. Excellent agreement was found between the experimentally measured reactivity of these slab cores, and that predicted by the Sofocate-Muff-Flip-PDQ routines being used in the physics analysis of the core. This agreement is shown in Tables 3.11 and 3.12. Only the experiment involving a fully inserted boron stainless steel control rod in a slab core has not been

TABLE 3.11 SUMMARY OF RESULTS ON BOILER SLAB EXPERIMENTS

Enrichment U-235 (per cent)	$\frac{W}{U}$	Loading (Kg U-235)	Slab Size (in.)	Void Content (percent)	K_{eff}	
					Experimental	Calculated
1.8	3.8	25.2	20.4x27.7x72	0	1.0025	0.996
1.8	5.7	26.4	21.7x34.9x72	0	1.0025	1.0081
1.8	4.8*	33.8	21.7x44.1x72	15.2	1.0019	1.0053
1.8	3.1*	33.3	21.7x35.0x72	18.5	1.0025	0.9994
1.59	3.8	37.7	21.7x44.1x72	0	1.0010	0.9973

* Voided

TABLE 3.12 TEMPERATURE EFFECTS (1.8 per cent enrichment)

$\frac{W}{U}$	Slab Size (in.)	Temperature Coefficient (percent $\Delta k/k \times 10^3$)		Reactivity Swing from 68 F to 180 F (percent $\Delta k/k$)	
		at 70 F	at 160 F	Experimental	Calculated
3.8	20.4x27.7x72	-0.6 $\pm$ 0.2	-4.4 $\pm$ 0.2	-0.33	-0.47
5.7	21.7x34.9x72	0.0 $\pm$ 0.2	-0.6 $\pm$ 0.2	-0.1	-0.27

fully analyzed at this time. This will be reported in the next quarterly report.

3.3.1 SUPERHEATER-BOILER CORE SLAB EXPERIMENTS

Phase 2 of the Pathfinder critical experiments consisted of measurements on adjacent slabs of superheater and boiler fuel elements. A typical core configuration is shown in Figure 3.16. The boiler core elements consisted of 9 x 9 lattices of 1.8 per cent enriched fuel rods, which have been previously described.* Each superheater fuel element consists of two fuel tubes and a poison rod contained in a 9-ft long 1.150-in o.d. stainless steel tube. The outer tube is water tight so that moderator can be excluded or included in the interior of the element. During operation, the spaces outside the element is occupied by water moderator. The fuel tubes are of 0.72-in. and 0.47-in o.d. Each fuel tube has a 0.016-in meat clad on both sides with 0.016-in stainless steel. The total U-235 loading (93.35 per cent enrichment) is 81 grams per fuel element. The active fuel length is 6 ft. The fuel tubes are assembled concentrically about a 0.25-in o.d. rod of 0.17 w/o natural boron in stainless steel. Any or all of these components may be used in the make-up of the fuel element. The superheater lattice is square on approximately 1-1/4-in centers.

Experimentation on the boiler-superheater slab lattices consisted of the measurement of critical sizes with the superheater fuel element voided and flooded, the worth of the removal of the central boron

* ACNP-600 I

Figure 3.16 Core arrangement for boiler-superheater slab experiments.

Figure 3.17 Radial flux traverse with poisoned superheater - slab geometry.

poison rod, and flux distributions in the core regions. A typical measurement of the flux distribution across this slab arrangement is shown in Figure 3.17. It is seen that the flux is highly depressed in the superheater region, giving it a very low statistical weight for this core arrangement. Because of this low superheater statistical weight and inherent uncertainties, the reactivity changes due to flooding, poison removal, and superheater size were small, making analysis of the experiments difficult. Therefore, additional experimentation on the boiler-superheater slab was discontinued, and experiments with a full-core mock-up, where the statistical worth of the superheater can be made significant, were made.

3.3.2 FULL CORE MOCK-UP

Phase 3 of the Pathfinder critical experiments involves the full core mock-up of 428 superheater elements and 96 boiler elements. The lower half of the boiler core contains a 9×9 lattice of fuel rods and the upper half, an 8×8 lattice of fuel rods per each 5.2-in square fuel box. The initial aim of the full-core experiments is to determine the full core excess and total control rod worth for the cold clean reactor with a given boiler enrichment and superheater loading. The steps to achieve this are as follows:

- 1) build a full core loading,
- 2) hold-down the core excess reactivity with a uniformly distributed poison of known reactivity worth,
- 3) remove the poison and hold-down excess with the required control rods, and
- 4) measure the worth of the remaining rods by the rod-drop method.

The first full core loading built was with 1.8 per cent enriched fuel in the boiler and a full fuel and central poison-rod loading in the superheater. Figure 3.18 shows a measured radial flux distribution for this configuration taken through the superheater and 9 X 9 boiler section. It can be seen that the statistical worth of the superheater is very low for this case.

Core excess measurements were made by uniformly distributing 520 grams of natural boron in the form of 0.01-in thick B_4C -Al sheets, throughout the boiler core. It was determined from these measurements that this loading did not have adequate excess reactivity for good total control rod worth measurements.

To increase the core excess and enhance the worth of the superheater, all of the boron stainless steel poison rods were removed from the superheater. With this configuration, a complete core flux traverse was taken. Figure 3.19 shows a quarter-core section with the traverses indicated. Figures 3.20 through 3.24 show the flux distributions as measured by these traverses.

The effect of the control rod bank on the total activation and episcadium axial flux distribution is shown by Figure 3.20 and 3.21, respectively. The prominent peaks in the total activation distribution are caused by the water channels between fuel bundles. Figures 3.22 through 3.24 show horizontal traverses taken through various sections of the core. In the superheater, a flux dip is seen

at the control rod channel, an air filled stainless steel guide section. In the boiler core, the flux is peaked at control rod channels, which are water filled aluminum guide sections. These flux distributions are being used for comparison with calculated two-dimensional (X-Y and R-Z) flux distributions, for the same lattice, to determine the adequacy of the theoretical treatment.

The final step in building a full core excess for control rod worth measurements was to homogeneously add fuel pins of higher enrichment to the reference 1.8 per cent loading. This resulted in an average enrichment of 1.89 per cent in the boiler core. The loading of natural boron in the form of B_4C in aluminum was increased to approximately 950 grams in the boiler core. Criticality was achieved with this loading with all control rods removed.

The reactivity worth of the boron was then measured, incrementally, and compared to the worth as predicted by one and two-dimensional calculations on the lattice. It was determined that the 950 grams of boron was holding down 10 per cent in reactivity.

The final step in measuring the control rod worth will be to replace the uniform boron poison by the control rods, achieve criticality in as uniform a rod pattern as possible, and measure the remaining rod worth by the drop method. This will be reported in the next quarterly report.

Figure 3.18 Radial flux traverse, 9 x 9 boiler section, poisoned superheater

Figure 3.19 Quarter-core section showing flux traverse locations.

Figure 3.20 Axial flux traverse, boiler section.

Figure 3.21 Axial Epi-Cd flux traverse, boiler section.

Figure 3.22 Cross-core flux traverse, 9 x 9 boiler section.

Figure 3.23 Radial flux traverse, 9 x 9 boiler section.

Figure 3.24 Radial flux traverse, 9 x 9 boiler section.

**4. RADIOACTIVE GAS REMOVAL AND
STORAGE SYSTEM AND ION EXCHANGER**

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4. RADIOACTIVE GAS REMOVAL AND STORAGE SYSTEM AND ION EXCHANGER

The objectives of this project are as follows: 1) to investigate the corrosion properties of the reactor structural materials to insure proper material specifications for the Pathfinder reactor; 2) to develop an improved, reliable recombiner, a radioactive gas removal and storage system, and ion exchangers requiring minimum maintenance; 3) to study the processing requirements of the spent fuel assemblies; and 4) to develop a suitable handling system for radioactive spent resins.

4.1 GASEOUS WASTE DISPOSAL SYSTEM

A gaseous-waste disposal system for Pathfinder has been designed (Figure 4.1). Off-gas from the condenser will flow through the air-ejector-recombiner system through a three-minute decay pipe to a high-level alarm monitor. From this monitor, the gases will flow through a baffled decay tank, through an absolute filter, and then to the stack.

If the high-level alarm monitor indicates high-activity gases from the three-minute decay pipe, the gases will be held up and compressed in the baffled holdup tank for about 12 hours. A second tank, operating in parallel with the baffled holdup tank, will also provide 12 hr of holdup for compressed gases. After an average 12-hr decay, the gases will be released to the atmosphere. If necessary, the plant could be shutdown during this period.

Equipment and tank vents and building ventilation air will flow to the stack. A continually recording monitor at the stack will record the activities discharged.

- (1) HIGH ACTIVITY ALARM MONITOR
(2) CONTINUOUSLY RECORDING MONITOR

Figure 4.1 Gaseous waste disposal system. (A-C Dwg. 43-024-227)

4.2 RECOMBINER

The recombiner will be located after the second-stage jets and before the after-condenser of the two-stage steam-jet air ejectors. The recombiner will be of the catalytic bed type. It will recombine the dissociated hydrogen and oxygen in the off-gas with 99.9 per cent efficiency. The catalyst-bed is a high nickel alloy support coated with precious metals.

4.3 REACTOR WATER PURIFICATION SYSTEM

Two, element-type, precoat filter units will be used before the ion exchangers in the Pathfinder reactor water purification system. One filter unit will be operated and one will be placed in standby. The normal system flow is 61.5 gpm. One unit should be able to handle a

maximum flow of 250 gpm. To insure adequate filter capacity at high flow rates, a freshly precoated filter will be put into operation immediately preceding high flows. Both units can also be used in parallel should this be necessary.

The operating cycle of the filters will be determined by the filtration load. Initial operation will be to a maximum pressure drop of 15 psi. This pressure drop should be obtained after approximately 30 days of operation. If a shorter than desired operating cycle results, the allowable pressure drop may be increased up to 25 psi.

The following is a description of the equipment in the filter system (Figure 4.2). Each filter contains 29 backup elements. Each element is 36 in. long with a nominal 4-in diameter. The filter vessels are 36 in. dia. and approximately 7 ft high. They are constructed of 304 stainless steel. The backup elements are 316 stainless steel. Gaskets will be metallic. The design is such that access may be gained to the backup elements without disconnecting piping.

The precoat tank will be used for mixing the precoat. A draw-down gage is used to determine when the proper amount of precoat has been pumped into a filter.

The precoat pump is used for pumping the precoat slurry into a filter and for recirculation through the filter. Recirculation is necessary not only to precoat filters but also to maintain the precoat in place before the filter is put into operation.

Initial operation of the filters will be with Solka-floc as the precoat media. Solka-floc is pure, finely divided wood cellulose.

Figure 4.2 Reactor water purification system prefilters.
(Courtesy of R. P. Adams Company Inc.) (A-C Dwg. 43-202-013)

The following operating procedure will apply regardless of system flow rate.

Assume filter 11 is in use. The maximum pressure drop of 15 psi has been reached. Valves A₁ and B₁ are open. All other valves are closed.

Procedure to precoat filter 12:

The mixer is started and water and precoat media is added to the precoat tank.

Valves K, F₂, H₂, and J are opened. The required amount of precoat slurry is pumped, with the precoat pump, into the filter. The precoat pump is shut off. Valve K is closed.

Valve L is opened and water is allowed to fill the system until a flow is seen at S₂. The top of the vent is the high point in the piping. Valves J and L are closed.

Valve E is opened. The precoat pump is started and recirculates through the filter until the water shows clear at S₁.

Filter 12 is now ready to use. The precoat pump must be used to hold the precoat on the backup elements.

Procedure to change filters:

Valves A₂ and B₂ are opened. Valves A₁ and B₁ are closed. The flow is now through filter 12.

Valves E, H₂, and F₂ are closed and the pump is shut off. Filter 12 is now in full operation.

Procedure to backwash:

Valves D₁, H₁, and V are opened. Air is applied to the filter until a water flow is no longer seen at S₃. Valve D₁ is closed.

The pressure in filter 11 is allowed to build up to 60 to 70 psig. Valve C₁ is opened. After two minutes, valves V, H₁, and C₁ are closed. The filter is now backwashed.

Procedure to flush waste-line:

Valves L, E, H₁, and D₁ are opened. When water is seen at S₃, valves L, E, and D₁ are closed.

Valve V is opened and air pressure in the filter is allowed to build up to 60 to 70 psig. Valve C₁ is opened. After two minutes, valves V, H₁, and C₁ are closed.

Filter II is now on standby and may be precoated the same as filter I2. Normally, the filter will not be precoated until just before it is needed.

The use of a precoat filter will allow remote operation and eliminate any close personal contact with radioactive filter media.

5. INSTRUMENTATION AND CONTROL

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5. INSTRUMENTATION AND CONTROL

The object of this project is to perform investigations and experimental work relative to the selection of reliable control and instrumentation for the Pathfinder reactor.

5.1 PLANT CONTROL

The Pathfinder feedwater control system and the reactor steam pressure control system will be furnished by Bailey Meter Co. The feedwater control system is basically pneumatically operated and uses three control elements; steam flow, feedwater flow, and reactor water level.

The feedwater control system compares reactor steam flow, compensated for pressure and temperature, to the magnitude of feedwater flow, which is compensated by temperature and biased by the constant purification flow.

The flow error and reactor water level are summed and both proportional and reset action is applied to yield a pneumatic valve position demand signal. By selecting proper proportional band and reset rate, the steam flow-feed flow difference signal will be the primary control signal, and level error will be the secondary control signal.

In the event that either the steam flow or feedwater flow signals are accidentally lost from the control system, a "wipe-out" circuit will remove the remaining signal thus converting the system to a single-element type consisting of the level error signal only.

In addition to balancing steam flow changes to feedwater flow changes, this control system has been designed to maintain reactor water level:

- (1) Within ± 1 in. of the set point under stable conditions over the load range of 20 per cent to 100 per cent of 616,000 lb/hr.
- (2) Within ± 6 in. of the set point during level variations produced by normal load changes.
- (3) Within ± 10 in. during abnormal load rejection and/or turbine tripout.

A low capacity feedwater flow by-pass control valve will be used during reactor startup, shutdown, and other non-operating conditions. This valve is manually controlled.

The pressure control system described below includes all the hardware necessary to automatically position the turbine inlet valves and/or the regulating dump valve for maintaining constant steam-line pressure during normal plant operation. Based on the facts presented in the previous two Quarterly Reports (ACNP-6001 and ACNP-6006), steam line pressure (P_2) (instead of reactor pressure (P_1)) will be used as the control signal. This decision will not affect the required control hardware which includes a P_1 transmitter that can be used in pre-operational testing of P_1 control. All the electrical components require 115 volt, 60 cycle A.C. and all amplifiers operate with vacuum tubes.

It has been proposed that the power supply for both the feedwater and pressure control systems be supplied from the same bus that furnished the reactor instrumentation power. In the event of a power failure, the D.C. battery block, normally charged by an M-G set, then provides A.C. voltage to the bus by driving the M-G set in reverse. Thus, the control systems are safe from major power failures.

As mentioned previously (ACNP-5915) the Pathfinder plant will be used for base load operation with no steam being bypassed to the condenser for maintaining pressure.

Pathfinder will have two controlled variables; steam-line pressure and reactor power. The control actions to be taken from a pressure error will be

- (1) automatic motion of the inlet and/or regulating dump valves
- (2) manual motion of the inlet and/or regulating dump valves.

Reactor power will be changed by:

- (1) Manual motion of control rods.
- (2) Manual change of recirculation rate by changing (remotely) the positions of the butterfly valves in the recirculation lines.

The butterfly valve-recirculation control can be used conveniently to manually vary power output from about 75 to 100 per cent. An advantage of this mode of control over rod reactivity control is that a near constant void distribution will be maintained in the core. The butterfly valve operators have been carefully selected so that recirculation rate changes will not cause reactivity insertion rates greater than that by withdrawing the most valuable rod (5 cents/sec). (See Sec. 2.2 for more details of butterfly valve and recirculation specifications and performance.)

With base load operation, the plant output will be controlled manually by the plant operator. Electrical output changes will lag recirculation and control rod changes by approximately a half-minute. Even when the butterfly valve or control rod positions are being changed, the pressure control system will regulate pressure well within ± 10 psi.

Figure 5.1 Reactor pressure control system (A-C Dwg. 43-401-128)
(Courtesy of Bailey Meter Co.)

5.2 PRESSURE CONTROL SYSTEM

The basic theory and philosophy of the pressure control system for the important phases of reactor and turbine operation is described below. Reference will be made to the associated Bailey control system drawing Figure 5.1. As implied before, the control system will control steam flow in balance with the reactor steam generation rate in the process of maintaining steam line pressure at its preset level of about 550 psia. This pressure set point, however, is readily adjustable over a range of 400 psi. All symbols below again refer to Figure 5.1. The Bailey pressure control system uses a standard signal range of -25 to +25 D.C. for 0 to maximum output.

5.2.1 NORMAL REACTOR-TURBINE OPERATION

The pressure transmitter, RP-1, is located downstream from the superheater exit inside the reactor building and furnishes an electrical signal proportional to pressure. This signal is matched against a manually adjustable set point, RP-11a, and the resultant voltage error is applied to the remainder of the control system through a proportional plus reset controller, RP-10a. When automatic pressure control (normal) is used, the Instantaneous Transfer Stations (RP-8) are on auto. The signal output of RP-10a is a valve position demand signal and is biased -12.5 by RP-11b and amplified by a gain of 2 by RP-10b to provide the signal in proper voltage range to the Electric-Pneumatic Converter. The resultant pneumatic signal, covering a range of 3-27 psi, is the actual control signal applied to the pneumatically driven turbine inlet valve positioner, RP-16. This

component utilizes the pneumatic control signal to provide a linear motion of 2 in. applied to the turbine reach rod. A 2 in. reach rod motion will full stroke the inlet valves. Thus, a ± 25 - to 0-V output range of the controller, RP-10a, corresponds to a 3 to 27 psi range of the pneumatic signal, which in turn corresponds to 0 to full open position demand of the turbine inlet valves.

In the unlikely event that steam is being generated in excess of the turbine capacity (about 100 per cent of rated 616,000 lb/hr), the excess steam will be throttled by the regulating dump valve. The inlet valve position feedback signal is thus at its "open" limit (110v - A.C.) and any increased output of RP-10a becomes the demand signal to RP-17. The signal output of RP-11c will then be limited to 25V D.C. and the signal output of the 3 point summer will stay constant at -25V. Thus, the

pressure control system has approximately 200 per cent rated steam flow capacity for maintaining constant pressure.

It should be noted that when automatic pressure control is being utilized, the turbine governor does not control the inlet valves (below 102 per cent rated speed). Biasing out the turbine governor is accomplished by adjusting the speed changer to 2 per cent above its normal position. In this range, the turbine speed is controlled by the frequency of the power system.

5.2.2 NORMAL REACTOR OPERATION - TURBINE SPEED CONTROLLED

This condition exists when the Pathfinder Plant is disconnected from the electrical system and is furnishing power only for the station load.

Thus, the instantaneous selector station, RP-8a, is automatically switched to manual, and the turbine load limit is adjusted to handle the house load. The speed changer is then set to govern turbine speed at 100 per cent rated speed. During this operation, the pressure can be automatically regulated by the regulating dump valve. Thus, the signal output of RP-11a does not change the inlet valve position since RP-8a is then on manual. Instead its output is compared to the fed-back inlet valve position signal and the resultant error becomes the position demand signal for the regulating dump valve. In the event that manual pressure control is desired during this condition, RP-8b can be placed on manual and the operator can manually position the dump valve by watching the pressure indicator.

5.2.3 TURBINE OVERSPEED

In the event that the generator becomes electrically disconnected from the system, turbine speed will increase due to the excess steam entering the turbine. When the speed reaches 102 per cent, the turbine valve mechanism has the protective feature of automatically closing the inlet valves to prevent a serious overspeed (>110 per cent rated). During this rollback period (which is a minimum of 1 sec.), the variable auto-transformer will continuously transmit the inlet valve position in the feedback path and will furnish a decreasing signal corresponding to the decreasing inlet valve position. Thus, the feedback signal is responsible for opening the regulating dump valve to the necessary position to handle the existing steam flow. In case there is any slight mismatch between inlet valve and regulating dump valve positions, the resultant pressure error will send

an additional signal to the regulating dump valve to maintain the set pressure. As the instant 102 per cent speed is reached, the instantaneous selector station, RP-8a, is automatically switched to manual by an electrical signal transduced from the turbine governor. This action allows the turbine governor to have control of inlet valve position to maintain rated speed for the remaining station load on the generator.

The same turbine overspeed signal is applied to the solenoid valve in the pneumatic (3-27 psi) line at 102 per cent speed to position the reach rod at its minimum set position. The turbine speed will then settle out to slightly above rated speed in 2-3 seconds.

5.2.4 TURBINE TRIPOUT

Turbine tripout can be caused by serious overspeed (>110 per cent), turbine faults, and generator faults. These conditions cause the turbine stop valves to trip closed in about 0.35 sec cutting off all steam to the turbine thus protecting the turbine-generator from serious damage. When this stop valve action is initiated, a signal with turbine tripout intelligence will be sent to the auxiliary relay module, RP-12, thus opening the inlet valve position feedback circuit. When this action takes place, the valve position demand output signal of RP-11a will instantaneously be felt at the regulating dump valve with a magnitude corresponding to the previous inlet valve position. Again, any pressure error developed during this tripout period will result in an additional signal to RP-17 to return steam line pressure to the set point. The regulating dump valve will be positioned by a high capacity Compudyne Actuator with a 3000 psi oil supply

which can open the dump valve in a minimum of approximately 0.35 sec. The characteristic of the dump valve will be matched (in reverse) approximately to the turbine stop valve characteristic so as to prevent a large pressure swing (>20 psi) during the tripout period.

5.2.5 TURBINE STARTUP - SHUTDOWN

During turbine startup when the reactor is being brought up to rated pressure, warming steam will be sent to the turbine with all the instantaneous selector stations set for manual pressure control. (See ACNP-6006, Sec. 8.3 - Procedure for Turbine Startup and Plant Operation). If it is desired to automatically hold the steam line pressure constant for short time intervals while warming the turbine and raising reactor pressure, RP-8b can be switched to auto while RP-8a is on manual control of the turbine inlet valves. The controlled pressure can then be raised (or lowered) by manually changing the pressure set point. When the turbine is manually loaded to 10 MW and the speed changer set to the 102 per cent speed position, RP-8b is switched to manual and RP-8a switched to auto. Thus, pressure is automatically controlled while turbine steam flow is increasing and the regulating dump valve is being manually closed. After reaching the closed position, the regulating dump valve can be switched to auto and the turbine can be loaded to full power under automatic pressure control.

For turbine shutdown, the above procedures will occur approximately in reverse order to the point where the inlet valves are placed under

governor control. With RP-8b still set on auto and practically all the steam flow passing through the dump valve, the turbine trip valves will be manually tripped. All steam will then flow through the regulating dump valve and pressure will be automatically controlled at set point until the reactor is shut down.

PART B

POST COSTRUCTION R&D

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1. INITIAL STABILITY AND PERFORMANCE TESTS

The initial objective of this project is to 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. This equipment will then be used to conduct oscillator tests in Pathfinder to verify dynamic performance calculations and to determine experimentally the stability of the reactor system.

Detailed analysis of the various types of proposed oscillator rods have essentially eliminated the gear-type because of high dynamic loads and the cam-type because of the high coefficient of friction. A cylinder-type oscillator rod with special fuel elements was selected for Pathfinder. Built-in amplitude variation was abandoned in favor of the limited variation obtainable by control rod programming. It is anticipated that variation by a factor of 1.5 will be obtained by this method.

The design objective is to oscillate 10 cents worth of reactivity. Distributing this over the length of the core will result in minimum loss of fuel, i.e. six pins from each of the four adjacent fuel elements. The resultant diameter of the rotating cylinder will be about 2-3/4 in. and will contain about 200 sq. in. of oscillating neutron poison, shaped so as to give a sinusoidal signal when rotated in a stator with appropriate sectors of neutron poison.

A list of standard components is being assembled in order to reduce the number of special designs that would be required. A layout of the oscillator rod, incorporating these standard components will be made shortly.