MASTER

**PATHFINDER ATOMIC POWER PLANT
TECHNICAL PROGRESS REPORT**

JANUARY 1960 - MARCH 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**

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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

June 15, 1960

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JANUARY 1960 - MARCH 1960

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FOREWORD

This is the tenth 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.
Otter-Tail Power Company
St. Joseph Light and Power Co.
Wisconsin Public Service Corp.

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DESIGN DATA

CRBR WITH NUCLEAR SUPERHEATER

Plant

Power, boiler region.....	164,000 kw
Power, superheater region.....	39,700 kw
Steam flow at rated power.....	615,000 lbs/hr
Total core power.....	203,700 kw
Gross electrical capability.....	66,000 kw
Net electrical output.....	62,000 kw
Net efficiency.....	30.5 per cent
Steam outlet pressure.....	540 psig
Reactor operating pressure.....	600 psig
Temperature, boiler region.....	489 F
Outlet temperature, superheater region.....	825 F
Gross heat rate.....	10,370 Btu/kw-hr
Reactor building size.....	50 ft dia x 120 ft

Reactor

Vessel size.....	11-1/2 ft x 31-1/2 ft
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.....	130,000 Btu/hr-ft ²
Average heat flux, superheater region.....	80,000 Btu/hr-ft ²
Maximum heat flux, boiler region.....	462,000 Btu/hr-ft ²
Maximum heat flux, superheater region.....	250,000 Btu/hr-ft ²
Recirculation rate.....	60,000 gpm
Recirculation pump power.....	794 kw

1. FUEL ELEMENT RESEARCH AND DEVELOPMENT

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1.1 FUEL MATERIAL CLADDING, BONDING, AND IRRADIATION TESTING

1.1.1 Cladding

1.1.1.1 8001 Aluminum Alloy. Dynamic Corrosion Tests.

Two methods of reducing the corrosion rate of 8001 aluminum alloy are being investigated through tests in dynamic corrosion test loops 2 and 3. The test in loop 2, involving an excess (1) aluminum section, was completed. The ratio of the aluminum surface area to water volume was 134 cm²/liter. The average test conditions were as follows: 500 F, 1200 psi, pH 6.0, and flow velocity 28 fps. The samples were inspected at about ten-day intervals over the total exposure period of 107.0 days. The test shows a considerable improvement in both sample appearance and corrosion rate.

The average corrosion rate of samples for this test are shown in Figure 1.1 with data from a previous test in which the excess aluminum section was placed in the ion exchange (2) vessel. These improved results indicate that the loop

1. Pathfinder Atomic Power Plant, Technical Progress Report, October 1959 - December 1959, Allis-Chalmers Report ACNP-6001, p. 1

2. Pathfinder Atomic Power Plant, Technical Progress Report, July 1959 - September 1959, Allis-Chalmers Report ACNP-5924, p. 2

Figure 1.1 Dynamic corrosion tests. The average corrosion rate of X8001 aluminum alloy with an increased area-to-volume ratio in the test loop was measured. The excess aluminum section was placed in the ion exchange vessel in Test 3 and directly upstream from the sample coupons in Test 4.

(A-C Dwg. 43-024-143)

Figure 1.2 Dynamic Corrosion Tests. The corrosion rate of prefilmed X8001 aluminum alloy was measured. The samples for the two tests were prefilmed at different temperatures.

(A-C Dwg. 43-024-144)

component arrangement and location of the excess area in the loop has a very definite bearing on the corrosion rate. The average overall corrosion rate of samples during the 107.0 days of the test was 2.5 mils/year. The average long-term rate during the 14 days before the last inspection was 7.2 mils/year.

A new test was started in loop 2 in order to ascertain the reproducibility of these low corrosion rates. The test conditions and loop component arrangement are the same as the previous test.

(3)

The test of statically prefilmed samples in loop 2 is being continued. The test conditions are: 500 F, 1200 psi, pH 6.2, and flow rate 27.8 fps. The sample coupons had been prefilmed in an autoclave at 600 F.

The average corrosion rates obtained thus far for this test are compared to the corrosion rates of samples prefilmed at 500 F in Figure 1.2. The initial corrosion stage in both tests is somewhat prolonged when compared to the corrosion rate of samples that were not prefilmed. After the break-off point, however, the long-term rate increases to about the same rate that was obtained with samples not prefilmed. At the break-off point, the prefilm coating began peeling or spalling off. The average overall corrosion rate for the 77.8-day test period thus far completed was about 9.6 mils/year. The average long-term corrosion rate for the last twelve days of this period was 21 mils/year.

3. Ibid., p.2

1.1.2 Irradiation Testing

1.1.2.1 Boiler Fuel. Capsule Tests. Capsules 1, 2, and 3 were exposed to a near-full cycle in January and a full cycle in February. The cladding temperatures during the January cycle reached a peak of 390 to 430 F. At the end of the February cycle, the temperature reached 520 F, exceeding the design parameter of 500 F for the first time. The February cycle was a long cycle, and the control rods had been raised to obtain a higher neutron flux than was obtained in previous cycles.

During the first three cycles, the third of which was made in December, the cladding temperatures failed to reach the design temperature. Capsule 4 was therefore withdrawn from the experiment, so that an examination could be made to determine the cause of the low temperatures. Post-irradiation examination of fuel specimens indicated no physical or dimensional changes in the aluminum cladding. Micro-sections of the irradiated fuel revealed no grain growth or center melting, which indicated that center temperatures less than 1700 °C had been obtained. Burnup of uranium-235, as determined by the amount of cesium-144 present, was 3130, 3080, and 2410 MWD/Ton for the 4.5-, 6-, and 7-per cent enriched samples, respectively.

On the basis of these results, capsules 1 and 3 were placed in lower positions in the facility for the March cycle, so that maximum heat fluxes of 500,000 and 750,000 Btu/hr-ft², respectively,

would be obtained. Capsule 2 was placed in its original position. Grain growth and some center melting of fuel pellets in Capsule 3 is expected during the cycle.

Subassembly Tests - Fabrication of Test Assemblies. Two boiler fuel element test assemblies were fabricated for irradiation tests. The subassemblies consisted of a 1-7/8-inch square 8001 aluminum alloy box containing nine fuel rods in a square array. ^(4,5) The fuel rods consisted of uranium dioxide fuel pellets clad with 8001 aluminum alloy.

Subassembly No. 1, which had been completed, was cleaned and autoclaved at 600 F and 1500 psi. During autoclaving, catastrophic corrosion occurred in the nozzle region, as may be seen in Figure 1.3. Since the autoclave conditions were not stringent enough to cause this failure, several, more probable causes were investigated: 1) stress corrosion, 2) corrosion due to some impurity, and 3) crevice corrosion.

Eight samples of aluminum were cleaned in the same manner as the subassembly. This included dipping in trichlorethylene degreasing agent. Two samples were used as control samples; two were stressed; two were bent so that crevices were produced; and two were bent to produce crevices, and then dipped again in trichlorethylene. Of these samples, the two that were bent

4. Ibid., Fig. 1.4

5. Ibid., Fig. 1.3

Figure 1.3 Catastrophic corrosion of boiler element test assembly. Trichlorethylene was trapped in crevices formed in joining the nozzle to the aluminum box. During autoclaving at 600 F, 1500 psi, a reaction between trichlorethylene and aluminum resulted in catastrophic corrosion. (M & C Photo B5736WW)

and dipped in trichlorethylene exhibited excessive corrosion when autoclaved at 600 F and 1500 psi.

In order to verify these results, a section duplicating the end of the aluminum box and nozzle to which it is attached with eight cap screws was fabricated and tested. This test showed that trichlorethylene is trapped in the crevices between the box and nozzle and stainless steel backup strips, and that excessive corrosion results during autoclaving. No previous reference to this reaction has been found.

Subassembly No. 1 was disassembled and examined. No white corrosion product was observed on any of the fuel pins, under the straps, or on any other area of the rod bundle. Corrosion product was found in crevices under the blisters covering the strap

expansion slots. After this examination, the fuel bundle was re-welded into another box, the end fittings were installed again, and the entire assembly was autoclaved. After autoclaving, no unusual corrosion was observed.

Subassembly No. 2 was completed and autoclaved. During autoclaving, an inadvertent pressure excursion, twice the specified pressure, occurred. The fission gas spaces collapsed making subassembly No. 2 unfit for further use.

1.1.2.2 Superheater Fuel. Highly enriched uranium dioxide stainless steel cermet. Highly enriched uranium dioxide stainless steel cermet is planned for the first 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.

Data was obtained from Battelle Memorial Institute (BMI) and Knolls Atomic Power Laboratory (KAPL), both of whom have made detailed theoretical studies on this subject since 1950. A creep-mechanism failure analysis proposed by BMI and a pressure-strain analysis proposed by KAPL are being used to estimate Pathfinder fuel life.

Figure 1.4 Heat Transfer Loop. The 10-8-6 pin test section, shown slightly to the right of center, will be used to obtain two-phase pressure drop and void fraction data for an aluminum clad boiler fuel element. The superheater test section to the left of center will be used to determine heat transfer and fluid flow characteristics of superheater fuel elements having annular flow channels. (A-C Photo 207014)

1.2 HEAT TRANSFER AND FLUID FLOW INVESTIGATIONS

1.2.1 Heat Transfer Loop

The superheater addition of the heat transfer loop was completed, and instrumentation was installed. The addition is shown in Figure 1.4 to the left of center. The loop was cold pressure tested to 2250 psi, and insulated. A dummy superheater test section was placed in the loop for hot pressure tests.

The 10-8-6--pin test section was assembled and installed in the loop, and is shown in Figure 1.4 to the right of center. The inner structure of this test section is shown in Figure 1.5. This section will be used to measure two-phase pressure drops along the entire length of the test section at flow rates of 100, 120, and 140 gpm at power levels of 240, 280, 320, 360, and 400 kw. Data from the test will be used to determine the pressure drop variations across the Pathfinder boiler core. Void determinations will be made with a gamma-ray attenuation, void-measuring device. This data will be applicable to either aluminum or zirconium-clad boiler fuel.

The high-enrichment superheater test section is being assembled and will be installed. The partially assembled section is shown in Figure 1.6.

1.2.2 Boiler Fuel Element

1.2.2.1 One-Pin Burnout Tests. Five burnout tests with the one-pin test section were made during the quarter at heat fluxes of 260,000, 390,000, and 680,000 Btu/hr-ft², which are 2, 3, and 5 times higher than average reactor heat flux. All tests were conducted at 600 psi without subcooling and with water velocities less than 1 fps. The hydraulic diameter of the test section was 0.875 inch.

In the last test run, wherein a heat flux of 680,000 Btu/hr-ft² was attained, the pin melted. Since the temperature rose gradually, boiling burnout was not indicated. The melting may have been caused by a flaw in the pin or a stagnant burnout.

Results of the test thus far indicate that the boiler elements in the reactor will not fail due to boiling burnout. The pin melted at a heat flux 30 per cent higher than the maximum boiler heat flux. The test conditions correspond, moreover, to a reactor condition where all recirculation pumps are stopped, and the reactor is operating at 30 per cent above rated power without scrambling.

More one-pin burnout tests with forced circulation are planned. Test sections with hydraulic diameters of 0.445 and 0.630 inch will be tested.

Figure 1.5 10-8-6--pin test section. Flexible cables connect the top of the test section (bottom left) to the upper electrode and allow thermal expansion of the test section. The box is made in three sections that, when installed, are electrically insulated from the test section. Pressure taps are shown on the 6-pin box, and holes for thermocouples are shown on the 8-pin box. The set screws are used to space the sides of the box from the pins during assembly. The entire section is installed in a tubular housing for tests in the heat transfer loop.

(A-C Photo 206873)

Figure 1.6 Annular superheater test section. Two electrodes on the top of the test section (left) permit control of current through each of the simulated fuel tubes. The outer tube is concentrically spaced from the inner tube by three parallel, enamel-coated wires. Steam will be passed through the annulus formed between the tubes. Eleven thermocouples will be used to monitor temperature. The entrance and exit pressure will be measured through pressure taps. The entire section is placed in a tubular housing for tests in the heat transfer loop.

(A-C Photo 207013)

Figure 1.7 Single-phase flow tests. The friction losses through the boiler test assembly were calculated and measured. The friction losses measured are about 50 per cent higher than calculated. This is probably due to the relatively rough corrosion film on the aluminum tubes. (A-C Dwg. 43-024-192).

Figure 1.8 Single-phase flow tests. Calculated and measured entrance losses for the boiler test assembly are compared. The decrease in experimental values is attributable to friction effects that are not considered in basic calculations. (A-C Dwg. 43-024-192).

1.2.2.2 Low-Pressure, Air-Water Tests. Extensive single-phase flow data with the boiler test subassembly were obtained. The friction losses expressed as the ratio between experimentally determined losses and losses calculated for drawn tubing are plotted as a function of Reynolds number in Figure 1.7. The higher friction losses measured are probably due to the relatively rough corrosion film on the aluminum tubes.

Calculated and experimentally determined entrance losses are plotted as a function of Reynolds number in Figure 1.8. The calculated values were obtained by the same methods used in calculating Pathfinder boiler fuel entrance losses. It should be noted that the measured entrance losses are not as severe as calculated showing that some degree of conservatism is present in the calculations. The decrease in the ratio of ΔP -to- $\rho V^2/2g$ is attributable to friction effects that are not considered in the basic calculations.

Although some air-water flow data was obtained, its usefulness is reduced by apparent problems with the measurement of void fractions.

1.2.2.3 Fuel Rod Vibration Test. A test loop was built to determine the amplitude and frequency of vibrations caused by flow of air and water through a boiler fuel element; and testing was started. The hydraulic diameter, water velocity, void

fraction, and rod EI (product of Young's Modulus and the moment of inertia), length, and weight will be varied during these tests. The rod end-fixity and diameter will be constant. End caps similar to those shown in Allis-Chalmers Report ACNP-6001, Figure 1.12 will be used.

Four test sections incorporating the aforementioned variables were made of stainless steel tube with a 3/8 inch o.d. and a 0.020-inch thick wall. Three of the test sections are made of two rods screwed together, and thus simulate the two-section zircaloy-clad boiler fuel pins. Detailed characteristics of these test sections are given in Table 1.1.

Table 1.1 Test Section Characteristics

Test Section	Rod	Type of Fill	Length (inch)	Weight (lb/in)	EI (lb/in ²)	Natural Frequency (Min Undamped)
A	1	Solid	18.41	0.091	11,300	47
	2	Lead	18.25	0.042	12,200	50
B	3	Loose Lead	18.41	0.026	9,950	56.2
	4	Shot	18.28	0.025	9,770	57.9
C	5	Hollow	18.28	0.0075	10,300	108
	6		18.44	0.0074	10,300	107
D	7	Loose Lead Shot	36.23	0.026	9,800	14.7

Tests will be made at hydraulic diameters of 0.568, 0.494, and 0.369 inch and at water velocities of 10, 15, and 20 fps. The measurement of amplitude and frequency will be made by placing foil strain gages on the rods midway between supports.

The leads from the gages to the rod ends are strips of copper foil 0.001 inch thick and 1/8 inch wide. A Tatnall waterproofing compound is used to insulate the leads and gages from the water and tubing. This method appears to be working successfully.

1.2.3 Superheater Fuel Elements

Air flow tests were made through an annular section formed by two concentric tubes spaced by three parallel wire spacers; and through a 7-pin cluster section with scalloped (septafoil) flow tube and spiral wire spacers. Results of this test are shown in Figure 1.9. The friction factor for the annular section compares favorably with published data. The friction

Figure 1.9 Air-Flow test friction-factor correlations.
(A-C Dwg. 43-024-142).

factor for the 7-pin cluster section is about 32 per cent higher than for the annular section. Preliminary calculations were made and show that the rotational acceleration would increase the pressure drop through the 7-pin cluster element by 25 per cent. The remainder of the friction factor difference may be due to the roughness of the scalloped flow tube. This roughness occurred during fabrication and is clearly visible.

A more detailed study of the effect of rotational acceleration and of flow-tube roughness will be made.

1.3 FABRICATION OF FUEL FOR THE CRITICAL FACILITY

1.3.1 Superheater Fuel Elements

Final development work at M & C Nuclear Corporation indicated that original specifications on superheater fuel dimensions and homogeneity could not be met in time to meet the test schedule. The specifications were therefore relaxed as much as possible. Adequate fuel homogeneity and dispersion will still be maintained.

During the quarter, 231 fuel elements were delivered. Some of these elements were difficult to assemble and disassemble because the welds joining the two sections of the elements

overpenetrated. This caused an excessively tight fit between the concentrically spaced fuel tubes.

M & C Nuclear Corporation will ream out overpenetrations in delivered rods. The remaining 199 fuel elements will be delivered during the next quarter with this defect corrected.

1.3.2 Flux Monitoring Pellets

Thirty flux-monitoring, wafer pellets 0.080 inch thick and 0.338 inch in diameter were fabricated for critical experiments. The wafers were made from boiler fuel pellets from which the aluminum cladding had been removed with 20 per cent sodium hydroxide solution. The pellets were then cut into wafers with the diamond cut-off saw and the sawed surfaces were sanded. Of the thirty wafers, ten each were made of natural enrichment, 1.8 per cent enriched, and 2.6 per cent enriched uranium dioxide, respectively.

Two perforated, flux-monitoring pellets of natural enrichment, two of 2.6 per cent enriched, and three of 1.8

Figure 1.10 Flux-monitoring pellets. (NP Photo 9A-0-3)

Figure 1.11 Reactor Core Layout for 5.312-inch Square
Zircaloy-2 Boiler Fuel Elements.
(A-C Dwg. 43-002-107)

per cent enriched uranium dioxide were fabricated for critical experiments. These pellets were made by cutting boiler fuel pellets in half, cutting five slots 0.020 inch wide by 0.020 inch deep over the length of the face, and cutting a matching half. Both the wafer pellets and slotted pellets are shown in Figure 1.10.

1.4 FUEL ELEMENT MANUFACTURING RESEARCH AND DEVELOPMENT

1.4.1 Alternate Core Layout

An alternate reactor core geometry was worked out assuming the use of a zirconium-clad boiler fuel element. This element is designed with a solid Zircaloy-2 fuel box rather than with the slotted box of the aluminum design. This box is designed to withstand the core pressure drop, and the boiler core shroud is not required to prevent flow from the core into the annular water reflector region. Therefore only a relatively simple structure was designed to support and align the upper fuel element hold-down and control rod guide tube structure. The lower half of the control rod guide tubes are formed by the sides of the fuel element boxes. Short cruciform stubs are mounted on the lower grid plate to align boiler fuel elements forming the guide tubes and to support the control rods in the down position. The boiler core layout is shown in Figure 1.11

Figure 1.12 Superheater Core Alternate Layout.
(A-C Dwg. 43-002-109)

An alternate superheater core geometry was worked out and is shown in Figure 1.12. This core includes 415 fuel elements of the reference design rather than the 429 used in the superheater with the aluminum boiler section. The fuel-to-water ratio is decreased to obtain a higher thermal neutron flux and to flatten the radial power distribution. The fuel element support tubes are of the reference design.

The four superheater control rods are each designed with thirteen poison rod fingers of 3/4 inch diameter ganged together in a cruciform array. The guide tubes are identical in design to the fuel element support tubes. The nuclear advantages of this design were previously described. (6)

1.4.2 Boiler Fuel Element

1.4.2.1 Zircaloy-2 Clad Fuel Element. Mechanical Design.

A detailed design of the fuel element nozzle assembly was completed. The nozzle flange is designed to provide a smooth transition from the round flow channel in the nozzle to the square flow channel in the box. The nozzle spring will be made of Inconel X, and the maximum stress is limited to less than

6. Pathfinder Atomic Power Plant, Technical Progress Report, October 1959 - December 1959, Allis-Chalmers Report ACNP-6001, p. 52

55,000 psi. The spring is compressed when assembled on the nozzle such that additional deflection of only 1-1/8 inch is required to support the weight of the fuel element. The spring rate is such that the differential expansion between the Zircaloy-2 box and stainless steel support for the upper hold-down structure plus tolerance buildup will be less than the deflection produced by the weight of the upper hold-down grid. This will insure that the fuel elements are properly held down at all times.

Fabrication. An inquiry package consisting of preliminary specifications and drawings were sent to nine possible vendors. An engineering estimate for one complete core plus spares and firm price bids on 18-inch long sample Zircaloy-2 clad fuel rods were requested.

Differential Expansion between Hot and Cold Rods.
Should a large variation in radial neutron flux occur across a given boiler fuel element, differential expansion between the Zircaloy-2 cladding tube of hot and cold fuel rods might be expected. The magnitude of the differential expansion was calculated assuming the average heat fluxes of 340,000 and 75,000 Btu/hr-ft² for the hot and cold rods, respectively. The temperature of the water was assumed to be the design temperature,

and the length of the rod was assumed to be 36 inches at room temperature. The film drop was calculated by the Jens and Lottes correlation. The results are given in Table 1.2 and indicate a maximum differential expansion of 0.006 inch, which is insignificant.

Table 1.2 Thermal Expansion of 36-inch Long Boiler Rods

Average Heat Flux (Btu/hr-ft ²)	Δt_{sat} °F	$\Delta t_{(a-b)/2}$ °F	$T_{\text{ave.}}^*$ °F	Thermal Expansion (inch)
75,000	16	10	515	0.058
340,000	24	53	566	0.064

* one half the temperature drop across the cladding.

Thermal Bowing. Since the average temperature of the hot and cold rods differ by only 40 degrees and since the heat fluxes assumed in arriving at this difference were very conservative, it would be reasonable to assume that the temperature difference from one side of a pin to the other side would not exceed about 20 F. Over the 18-inch length between tube sheets, the differential thermal expansion caused by this temperature difference would result in a thermal bowing that is negligible.

Stress Analysis of the Fuel Element Box. Stress analysis of the fuel element box show that column loadings and direct compression stresses at the fasteners are not critical. The stresses at the corners of the box near the bottom of the element are critical, however, because of an 18-psi pressure drop from the inside to the outside of the box. Strain-energy calculations of tensile stresses on the inside surface of the corners formed by the box sides indicate a maximum stress of about 11,000 psi. This stress is about the maximum allowable stress, since larger stresses would initiate creep deformation.

The stresses in an aluminum box were determined experimentally to verify the theoretical stress calculations made by the strain-energy method. The box, which is shown in Figure 1.13, was three feet long and similar in cross section to the boiler fuel element box. Strain gages were used to measure biaxial strain at the corner of the box and at the

Figure 1.13 Test setup for analysis of boiler fuel box.
(A-C Photo 207009)

center of the box side. The experimental stress values were calculated using the equation,

$$\sigma_1 = \frac{E}{1 - \mu^2} (\epsilon_1 + \mu \epsilon_2) \quad (1.1)$$

where, σ is a principal stress, E is the modulus of elasticity, μ is Poisson's ratio, ϵ_1 is the strain in the direction of the desired stress, and ϵ_2 is the strain perpendicular to ϵ_1 . All stress calculations assumed that the yield strength of the material was not exceeded.

The results of both experimental stress analysis and calculations are shown in Figure 1.14. The curve shows that below the yield strength, the theoretical stress values agree well with the experimentally determined stresses.

1.4.3 Superheater Fuel Element

1.4.3.1 Prototype High-Enrichment Superheater Fuel Element.

A program was initiated to develop and fabricate prototype high-enrichment fuel elements to Pathfinder specifications. A detailed set of fuel element specifications and a drawing of the proposed prototype fuel element were prepared and sent to suppliers requesting quotations for a development and manufacturing program. A drawing of the fuel element is shown in Figure 1.15.

Figure 1.14 Stress in boiler fuel element box test section under internal pressure. Values obtained experimentally and by calculations are compared. (A-C Dwg. 43-024-158)

Figure 1.15 High-Enrichment Prototype Superheater Fuel Element Assembly. (A-C Dwg. 43-500-785)

1.4.3.2 Superheater Support Tubes. Seal Test. Tests of various seal configurations and materials are being made to develop a seal that would be suitable for sealing the inner and outer superheater fuel element support tubes. Such a seal would permit inspection of the inside of the outer tube and the outside of the inner tube during shutdown. Seal configurations that were tested during the quarter are shown in Figure 1.16. The test setup and program were described in the last quarterly report. Results of several preliminary tests are given in Table 1.3.

Figure 1.16 Superheater Fuel Element Support Tube Seal Arrangements.
(A-C Dwg. 43-201-929)

Seal types I and II are balanced metal O-rings inserted with a slight force-fit. The single O-ring was unsatisfactory, but the double seal gave a satisfactorily low velocity of steam through the seal. Seal type III is a straight sided aluminum sleeve that takes advantage of differential thermal expansion. As the seal is heated by steam, its leakage decreases and then stops completely.

Table 1.3 Seal Leakage

Seal Type No.	Material	Test Conditions		Average Leakage	Remarks
		Inlet Pressure	Pressure Drop		
		(psig)	(psi)	(ml/hr)	
I	Stnl Steel	70	50	750	Leakage corresponds to 1.65 lbs/hr and a velocity of 1.95 fps through the stagnant steam space.
II	Stnl Steel	70	50	50	Corresponds to 0.11 lbs/hr and a velocity of 0.13 fps in the stagnant steam space.
II	Stnl Steel	95	85	180	
III	Aluminum	95	85		Stopped leaking after first half hour.
IV	Aluminum	95	95	0	
V	Aluminum	70	50	0	
V	Aluminum	95	85	0	
Va	Stnl Steel	95	85	200	maximum leakage during test

A force fit is the main feature of seal type IV. In seal type V, the pressure drop presses the tapered sleeve into the hole. Both seals take advantage of differential thermal expansion, and both were leak-tight. Seal type Va works similarly to type V except that thermal expansion is no advantage. This seal showed leakage of the same order as seal type II. Additional preliminary tests will be made using aluminum and silver-plated O-ring seals.

Figure 1.17 Spiral-Wire Wrapping Device. (A-C Photo 206314).

1.4.3.3 Spiral-Wire

Wrapping Device. A

device for wrapping spiral spacer wires around various diameter tubes was com-

pleted and operates

satisfactorily. The

device is shown in

Figure 1.17. The device

will wrap wire tightly

around tubes with diameters of 1/4 to 1 inch. The wire pitch can

be changed easily by changing the drive sprocket to one with a

different number of teeth. This device was used to wrap spacer

wires around the 0.234-inch diameter rods used in the 7-pin

cluster superheater test section for air flow tests. (See Sec. 1.2.3)

1.5 NUCLEAR HANDLING TOOLS, COFFINS, SHIPPING CONTAINERS

1.5.1 Boiler Fuel Handling Tools

The full-scale mock-up of one-eighth of the Pathfinder core was placed into the 40-foot deep tank, and tests of remote handling tools and procedures were made. A 1/2-ton crane was used to handle the dummy boiler fuel elements. At first, a simple hook was used with some success, but it was found that an element could possibly unhook and drop. Therefore, a locking device operated by a push-pull cable as shown in Figure 1.18 was attached to the hook. It was subsequently determined that a hook latching in a vertical position would be easier and faster to use.

The assist-pole for guiding elements into place was found to have limited usefulness. It was determined that a straight-line pull by the crane is not necessary to remove the elements from the grid plate.

1.5.2 Superheater Fuel Handling Tools

Two superheater fuel handling tools that were tested are shown in Figure 1.19. Tool I is a bayonet type that requires a twist to latch and unlatch. Tool II has a spring-loaded mechanism that is triggered to latch by the weight of the tool

Figure I.18 Boiler Fuel
Element Hook with Push-
Pull Locking Latch.
(A-C Dwg. 43-024-140)

Figure I.19 Superheater Fuel Element Handling Tools.
(A-C Dwg. 43-024-141)

and released by using a push-pull cable. Both tools were used on the end of the long handling pole. Tool II can also be used on the end of a cable.

A basket for holding several superheater fuel elements was made, but it was difficult to place elements into it.

1.5.3 Lighting Requirements for Refueling

Lighting tests were completed. An object resembling the top part of the reactor core and superheater structure was placed at the proper depth in the 40-foot tank. Lights were placed at various positions above it to study their effectiveness. Baffles were placed at the side of the tank to simulate the vessel opening in the floor of the shield pool. A schematic of the test set-up is shown in Figure 1.20.

The top of the boiler core was simulated by painting the outline of the fuel elements on a large piece of sheet metal. The superheater chimney was simulated by welding three oil drums together and attaching them to the sheet metal. The top of the top drum was cut out so that its bottom roughly coincided with the superheater tube sheet.

Two types of underwater lamps were used. One was a 1000-watt diving lamp that is relatively lightweight and is supported by its electric cable. The other lamp is a standard 400-watt swimming

pool light that is relatively heavy and requires a separate support. These lamps were mounted on a yoke that permitted adjustment of the beam angle.

Both types of lights were tested at various depths in the following positions: a) at the sides of the tank above the aforementioned baffles, b) at the sides of the tank below the baffles, which would simulate a position within the reactor vessel, c) in the center of the tank directly over the superheater section, d) at various distances from the center to the outside.

As a result of these tests, the following recommendations for reactor lighting are made:

a) Two 1000-watt lamps similar to those used in the test should be hung about 2-feet inside the reactor vessel, close to the sides, and 180 degrees apart during refueling operations. These lamps would best be hung from two booms that would allow them to be readily swung out of the way when not needed. Support cables should be mounted on something similar to a friction drum, so that they can be maintained at any position and easily raised or lowered.

b) A single lamp with a narrow beam should be mounted on the fuel handling bridge and be portable. The 400-watt lamp used in the test would give more than enough illumination, but

LAYOUT OF CORE LIGHTING TEST

Figure 1.20 Schematic diagram of core lighting test.
(A-C Dwg. 43-024-165)

might be too heavy. This light would be used to refuel the superheater as well as for spot-lighting any other area within the vessel that requires unusually good illumination.

c) Since the two 1000-watt lamps will be inside the reactor vessel, other lighting will be necessary in the shield pool, especially near the transfer carriage. Two alternatives would be to permanently mount lights at the sides of the pool or beneath the bridge.

d) To further increase ease of handling fuel, it may be desirable to turn off all lights above water in the reactor building, to eliminate surface reflections; and to use a viewing box to reduce surface disturbances caused by crane cables and tools.

1.6 LOW ENRICHMENT SUPERHEATER FUEL ELEMENT

1.6.1 Design Status

A research and development program is being conducted for a low enrichment superheater fuel element for Pathfinder. Three fuel element configurations are being considered. In one concept, seven fuel rods in a septafoil cluster are contained in

a scalloped flow tube. The fuel rods consist of solid cylindrical uranium dioxide pellets or a single solid uranium dioxide rod clad in stainless steel. A second concept includes a single annular fuel rod consisting of pellet bushings clad in stainless steel. The third concept includes a perforated fuel rod consisting of cylindrical fuel pellets penetrated by multiple flow channels.

The initial phase of this program includes a programmatic data survey and ceramic fabrication experiments and is directed toward the development of ceramic fuel.

1.6.2 Programmatic Data Survey

A minor effort is being continued on the review and evaluation of uranium dioxide pellet fabrication processes. Reported data is being applied in fabrication experiments. Information was obtained on methods of cold compaction, powder variables, the use of organic additives, sintering variables, and alternate fabrication processes.

1.6.3 Ceramic Pellet Fabrication

1.6.3.1 Depleted Uranium Dioxide Powder -- Shattuck Chemical Company. Powder Particle Size. As the size of particles composing a powder decrease, the surface area and

activity increase. Therefore, the uranium dioxide powder is being ball milled to reduce the size of particles before making pellets. The effect of ball milling on particle size is shown in Figure 1.21. Particle size is measured with a sub-sieve analyzer. Each batch of ball-milled powder is analyzed and the results shown are an average of several batches.

Results show that ball milling for 25 hours reduces the particle size but that successive ball milling to 64 and 128 hours does not reduce the size significantly more. A preliminary analysis of Davison depleted uranium dioxide powder is also given.

Figure 1.21 Effect of Ball-Milling on Particle Diameter of Uranium Dioxide Powder. (A-C Dwg. 43-024-193)

Pelleting and Sintering. Initial pelleting and sintering studies of Shattuck powder were completed; and the green and sintered densities are given in Figures 1.22 through 1.24. Detailed test data is given in Table 1.4. Curves A through L represent the green density of cold compacted pellets. Curves A' through L' represent the sintered density. Five different groups of pellets using a different binder were pelleted at various pressures. The green pellets were sintered in a hydrogen atmosphere at 1600 to 1700 C. The sintering cycle varied and includes independent variables that are not considered in plotting the curves.

The density of pellets made from as-received powder is shown in Figure 1.22. Of these pellets, those using Carbowax as binder were the best pellets having the best surface appearance, the highest fired density, and the smallest shrinkage due to sintering. Pellets made with 2.0 per cent Carbowax and 0.4 per cent Sterotex had slightly lower sintered densities but surface appearance and shrinkage was comparable. Pellets made with 0.4 per cent Sterotex had high sintered densities but also had a poor surface appearance and exhibited higher shrinkage. Pellets made with polyvinyl alcohol (PVA) with or without Sterotex had poor green strength and could not be sintered.

Of pellets made with powder ball-milled for 25 hours, those made with 2.0 per cent Carbowax were the best as may be seen in

Figure 1.23. The pellets made with 0.4 per cent Sterotex had a poor surface appearance and greater shrinkage. The highest green densities were obtained in pellets made with 2.0 per cent Carbowax and 0.4 per cent Sterotex, but the pellets fractured during sintering probably because of a furnace malfunction. As with as-received powder, pellets made with PVA had poor surface appearances and green strengths and could not be sintered.

Of pellets made with powder ball milled for 64 hours, those made with 0.4 per cent Sterotex had the highest sintered and green densities. Shrinkage was not excessive but surface appearance was inferior. Pellets made with Carbowax were the best with moderate shrinkage, good surface appearance, and high sintered densities. Pellets made with both Carbowax and Sterotex exhibited higher shrinkage. Pellets made with PVA were inferior from most standpoints.

Of all pellets, those made with 25-hour ball-milled powder with 2.0 per cent Carbowax were the best. Pellets pressed at 50 to 55 tsi produced the best sintered pellets with excellent surface appearances, 30 per cent shrinkage during sintering, and fired densities above 95 per cent of theoretical density.

The effect of sintering variables will be studied as soon as a furnace programmer is obtained. Pelleting studies of

(Continued on page 55)

Figure 1.22 Density of sintered and green pellets made from as-received Shattuck powder. (A-C Dwg. 43-024-194)

Figure 1.23 Density of pellets made from Shattuck powder ball-milled for 25 hours. (A-C Dwg. 43-024-195)

Figure 1.24 Density of pellets made from Shattuck powder ball-milled for 64 hours. (A-C Dwg. 43-024-196)

Figure 1.25 Cylindrical and bushing pellets made by pressing in a hydraulic die with Carbawax binder. (A-C Photo 207016C)

TABLE 1.4

DENSITY OF PELLETS MADE FROM SHATTUCK DEPLETED URANIUM DIOXIDE
POWDER AT VARIOUS FORMING PRESSURES

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES A - AS-RECEIVED DEPLETED OXIDE

Group 1 -- 2.0 per cent Sterotex, sintered 15 minutes at 1650°C.

26	5.71	52.1	7.25	66.3	88.2
39	6.46	59.1	8.19	74.8	94.0
52	5.87	53.6	7.75	70.9	86.9
62	6.93	63.2	8.71	79.7	93.7
76	7.18	65.5	9.00	82.0	92.9
88	6.63	60.5	7.99	73.1	87.0
100	7.38	67.3	9.03	82.6	92.2
126	6.96	63.5	8.37	76.5	88.6

Group 2 -- 0.4 per cent Sterotex, sintered 75 minutes at 1650°C.

26	6.16	56.3	9.23	84.1	93.8
39	6.41	58.5	9.50	86.6	88.3
52	6.72	61.4	9.63	87.7	88.3
62	6.84	62.5	9.67	88.1	88.0
76	7.13	65.1	9.81	89.5	92.9
88	7.28	66.5	9.90	90.2	91.7
100	7.24	66.1	9.71	88.5	91.0

Group 3 -- 2.0 per cent PVA

39	6.11	55.9
52	6.52	59.6
62	6.37	58.3
76	6.85	62.7
88	7.10	64.9
100	broken*	----

FRACTURED

DUE TO FURNACE MALFUNCTION

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent Theoretical (Calculated)	(g/cc)	Per Cent Theoretical (Calculated)	Per Cent Theoretical (Pycnometer)

SERIES A - AS-RECEIVED DEPLETED OXIDE (Continued)

Group 4 -- 2.0 per cent PVA and 0.4 per cent Sterotex.

39	6.26	57.2	FRACTURED		
52	6.56	60.0			
62	broken*	----	DUE TO FURNACE MALFUNCTION		
76	6.98	63.4			
88	6.81	62.3			

Group 5 -- 2.0 per cent Carbowax, sintered 90 minutes at 1650°C

26	6.24	57.1	9.14	83.3 ^s	91.7
39	6.55	59.8			93.8
52	6.77	61.9	9.62	87.7 ^L	94.0
62	7.04	64.9	9.97	90.8 ^s	93.2
76	7.18	65.7	9.91	90.4 ^s	94.7
88	7.30	66.8	10.07	91.8 ^s	93.8
126	8.13	74.3		FRACTURED	
139	8.13	74.3		FRACTURED	
113	broken*	----			

Group 6 -- 2.0 per cent Carbowax and 0.4 per cent Sterotex,
sintered 90 minutes at 1650°C.

26	6.33	57.9	9.36	85.3	92.1
39	6.71	61.4			92.0
52	6.95	63.6		FRACTURED	
62	7.05	64.5			
76	7.21	65.0			
88	7.40	67.7			
113	8.01	73.3			
126	8.07	73.8			
139	8.14	74.4			

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES B - 25 HOUR BALL MILLED DEPLETED OXIDE

Group 1 -- 0.4 per cent Sterotex, sintered 90 minutes at 1650°C.

26	broken*				95.5
39	6.96	63.7		FRACTURED	97.1
52	7.23	66.2	10.29	93.2 ^s	98.5
62	broken*				
76	7.37	67.4		FRACTURED	95.6
88	7.47	68.4			95.6
100	7.47	68.3			95.1
113	broken*				

Group 2 -- 2.0 per cent PVA.

26	6.28	57.5		FRACTURED	
39	6.32	57.8			
52	6.39	58.4			
62	6.50	59.4			
76	6.58	60.2			
88	6.64	60.8			
100	6.73	61.5			

Group 3 -- 2.0 per cent PVA and 0.4 per cent Sterotex.

26	6.04	55.3		FRACTURED	
39	broken*	----			
52	broken*	----			
62	6.20	56.7			
76	broken*	----			
88	6.49	59.4			
100	6.73	61.6			

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES B - 25 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 4 -- 2.0 per cent Carbowax.

26	6.97	63.8	FRACTURED	94.3
39	7.21	66.0		96.4
52	7.33	67.0		95.1
62	7.59	69.4		96.3
76	7.52	68.8		95.9
88	broken*	----		
100	7.84	72.2		95.2

Group 5 -- 2.0 per cent Carbowax and 0.4 per cent Sterotex.

26	7.08	64.6	FRACTURED	
39	7.32	66.8		
52	7.48	68.3		
62	7.61	69.4		
76	7.74	70.6		
88	7.82	71.3		
100	7.96	72.6		

SERIES C - 64 HOUR BALL MILLED DEPLETED OXIDE

Group 1 -- 0.4 per cent Sterotex, sintered 90 minutes at 1650°C.

62	7.36	67.1	10.18	92.9 ^S	93.9
76	7.56	68.9	10.19	93.5 ^S	94.2
88	7.68	70.0		Fractured	93.8
100	7.79	71.1		Fractured	94.4
113	7.82	71.3		Fractured	92.2
126	broken*	----			
139	broken*	----			

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES C - 64 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 2 -- 2.0 per cent PVA, sintered 4 hours and 15 minutes
at 1690°C.

62	6.59	59.2	9.10	82.9	85.4
76	6.70	61.1	9.25	89.3	85.6
88	6.70	61.2	9.20	83.8	85.4
100	6.85	62.5	9.16	83.5	86.4
113	6.83	62.2	8.95	81.5	85.0
139	6.93	63.2	9.02	82.2	85.2

Group 3 -- 2.0 per cent PVA and 0.4 per cent Sterotex, sintered
4 hours and 15 minutes at 1690°C.

62	6.60	60.1	8.66	78.9	83.1
76	6.44	58.7	8.71	79.4	83.6
88	6.55	59.7	8.68	79.1	84.0
100	6.71	61.2	8.64	78.8	82.0
113	6.80	62.0	8.55	77.9	82.2
139	7.34	66.9	8.78	80.1	84.5

Group 4 -- 2.0 per cent Carbowax, sintered 30 minutes at 1600°C.

62	7.38	67.3	9.47	86.3	89.5
76	7.46	68.0	9.72	88.7	90.7
88	7.61	69.4	9.78	89.1	91.3
100	7.70	70.2	9.84	89.7	90.8
113	7.74	70.5	9.68	88.2	91.1
126	7.90	71.5	9.60	87.5	89.3
139	7.91	72.0	9.51	86.7	88.9

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES C - 64 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 5 -- 2.0 per cent Carbowax and 0.4 per cent Sterotex,
sintered 30 minutes at 1600°C.

62	7.26	66.2	9.28	84.6	90.2
76	7.49	68.3	9.63	87.8	88.6
88	broken*	----			
100	7.57	69.0	9.70	88.5	91.6
113	7.56	69.0	9.48	86.4	87.5
126	7.70	70.2	9.44	86.0	88.7
139	7.70	70.2	9.41	85.8	86.9

SERIES D - AS-RECEIVED DEPLETED OXIDE

Group 1 -- 0.4 per cent Sterotex, sintered 4 hours and 15
minutes at 1690°C.

62	6.91	63.0	9.86	89.9	91.6
76	7.04	64.2	9.90	90.2	91.1
88	7.15	65.1	9.98	91.0	92.1
100	broken*	----	9.92	90.4	93.4
113	7.51	68.4	10.18	92.8	95.1
126	broken*	----		FRACTURED	
139	broken*	----			
152	broken*	----			

Group 2 -- 2.0 per cent Carbowax, sintered 4 hours and 15
minutes at 1690°C.

62	6.78	61.8	9.67	88.2	89.1
76	6.80	62.0	9.61	87.6	91.2
88	6.82	62.1	9.53	86.9	89.1
100	6.89	62.8	9.54	86.9	90.7
113	7.12	64.9	9.59	87.4	91.0
126	7.24	66.0	9.64	87.9	91.4
139	7.33	66.9	9.58	87.3	91.5
152	7.38	67.2	9.49	86.5	89.9

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES D - AS-RECEIVED DEPLETED OXIDE (Cont.)

Group 3 -- 2.0 per cent Carbowax and 0.4 per cent Sterotex,
sintered 4 hours and 15 minutes at 1690°C.

62	6.63	60.4	9.30	84.7	85.7
76	6.85	62.4	9.33	85.1	87.0
88	6.88	62.7	9.35	85.2	86.3
100	7.05	64.2	8.86	80.8	90.7
113	7.14	65.1	9.34	85.1	89.9
126	7.29	66.4	10.05	91.6	88.7
139	7.40	67.4	10.06	91.7	91.1
152	7.52	68.5	10.16	92.6	89.7

SERIES E - 64 HOUR BALL MILLED DEPLETED OXIDE

Group 1 -- 2.0 per cent Carbowax, sintered 16 hours at 1670°C.

70	7.64	69.6	FRACTURED		
70	7.68	70.0			
75	7.68	70.0			
75	7.67	69.9			
76	7.69	70.0			
76	7.75	70.6			
78	7.79	71.0			
78	7.73	70.5			
81	7.79	71.0			
81	7.82	71.3			

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density	
	(g/cc)	Per Cent	(g/cc)	Per Cent
		Theoretical (Calculated)		Theoretical (Pycnometer)

SERIES E - 64 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 2 -- 2.0 per cent Carbowax, sintered 16 hours at 1670°C.

70	7.63	69.6	FRACTURED
70	7.64	69.7	
70	7.65	69.7	
74	7.72	70.4	
74	7.70	70.2	
75	7.72	70.3	
75	7.71	70.3	
78	7.74	70.6	
78	7.69	70.1	
80	7.72	70.4	
80	7.73	70.5	

Group 3 -- 2.0 per cent Carbowax, sintered eight hours and thirty minutes at 1700°C.

70	7.72	70.4	FRACTURED
70	7.71	70.3	
75	7.76	70.8	
75	7.75	70.7	
76	7.79	71.0	
76	7.83	71.4	
78	7.87	71.8	
78	7.88	71.8	
81	7.91	72.1	
81	7.92	72.2	

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density	
	(g/cc)	Per Cent	(g/cc)	Per Cent
		Theoretical (Calculated)		Theoretical (Pycnometer)

SERIES E - 64 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 4 - 2.0 per cent Carbowax, sintered 8 hours and 30 minutes at 1700°C.

70	7.76	70.7	FRACTURED
70	7.76	70.8	
75	7.81	71.2	
75	7.78	71.0	
76	7.83	71.4	
76	7.82	71.3	
78	7.90	72.0	
78	7.84	71.5	
81	7.86	71.7	
81	7.85	71.5	

Group 5 - 2.0 per cent Carbowax, sintered 5 hours at 1690°C.

56	7.69	70.1	FRACTURED
56	7.73	70.5	
58	7.74	70.5	
58	7.74	70.6	
60	7.75	70.6	
60	7.76	70.7	
62	7.83	71.3	
62	7.81	71.2	
64	7.82	71.3	
64	7.80	71.1	

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent Theoretical (Calculated)	(g/cc)	Per Cent Theoretical (Calculated)	Per Cent Theoretical (Pycnometer)

SERIES E - 64 HOUR BALL MILLED DEPLETED OXIDE (Cont.)

Group 6 - 2.0 per cent Carbowax, sintered 5 hours at 1690°C.

56	7.89	71.0	FRACTURED		
56	7.78	70.9			
58	7.78	70.9			
58	7.80	71.1			
60	7.81	71.2			
60	7.82	71.3			
62	7.84	71.5			
62	7.82	71.3			
64	7.73	70.4			
64	7.86	71.6			

Group 7 - 2.0 per cent Carbowax, sintered 10 hours at 1700°C.

72	7.34	66.9	FRACTURED		
72	7.38	67.5	9.88	90.0	91.4
74	7.32	66.7	9.69	88.3	89.7
74	7.42	67.6	9.84	89.7	93.0

Group 8 - 2.0 per cent Carbowax, sintered 10 hours at 1700°C.

72	7.37	67.2	9.81	89.5	92.2
72	7.36	67.1	9.80	89.4	90.1
74	7.35	67.0	9.73	88.7	91.0
74	7.38	67.3	9.80	89.3	89.7
76	7.35	67.0	9.80	89.4	89.9
76	7.34	66.9	9.77	89.1	90.7
78	7.38	67.3	9.76	88.9	90.5
78	7.39	67.3	9.77	89.0	91.3
80	7.38	67.3	9.75	88.8	90.2
80	7.34	66.9	9.71	88.5	90.9

TABLE 1.4 (Continued)

Forming Pressure (tsi)	Green Density		Sintered Density		
	(g/cc)	Per Cent	(g/cc)	Per Cent	Per Cent
		Theoretical (Calculated)		Theoretical (Calculated)	Theoretical (Pycnometer)

SERIES F - 64 HOUR BALL MILLED DEPLETED OXIDE, BUSHINGS

Group 1 - 2.0 per cent Carbowax, sintered 1 hour at 1570°C.

60	7.52	68.6	10.13	92.3	94.0
70	7.61	69.4	10.23	93.2	93.2
70	7.59	69.2	10.11	92.2	94.9
80	7.65	69.7	10.02	91.3	93.9
80	7.67	69.9	10.13	92.3	94.2

SERIES G - AS-RECEIVED DEPLETED OXIDE***

Group 1 - 2.0 per cent Carbowax, sintered 1 hour at 1570°C.

40	7.31	66.6		FRACTURED	
50	7.39	67.3	8.91	81.2	92.8
60	7.53	68.6	9.04	82.4	92.1
70	7.81	71.2	9.26	84.4	93.5
80	7.72	70.4	9.25	84.3	92.9
90	7.85	71.6	9.33	85.0	93.9
100	7.91	72.1	9.38	85.5	92.9
110	7.94	72.4	9.34	85.1	93.0

Group 2 - 2.0 per cent Carbowax and 0.4 per cent Sterotex,
sintered 1 hour at 1570°C.

40	7.26	66.2	8.66	78.9	95.8
50	7.33	66.8	8.72	79.5	94.7
60	7.46	68.0	8.95	81.6	93.8
70	7.54	68.8	9.04	82.4	94.7
80	7.56	68.9	8.91	81.2	92.5
90	7.66	69.8	9.06	82.6	92.6
100	7.73	70.5	9.13	83.2	92.2

TABLE 1.4

FOOTNOTES

- * Striated, fractured, or powdered during pressing.
 - ** Lost during sintering due to furnace malfunction.
 - *** Beginning with Series G, depleted oxide from Davison Chemical Company was used.
 - s Small edge cracks and die marks on side following sintering.
 - L Large edge cracks and die marks on side following sintering.
-

128-hour ball-milled Shattuck powder are being made. Pelleting studies of powder from Davison Chemical Company are being made.

Bushing Studies. Pellet bushings were made using the same techniques used in making solid cylindrical pellets. Several bushings that were fabricated are shown in Figure 1.25. These pellets were made of Shattuck powder ball-milled for 64 hours with 2.0 per cent Carbowax. The characteristics were similar to those indicated by Curve J in Figure 1.24. Sintered densities of 90 to 94 per cent of theoretical density with forming pressures of 50 to 65 tsi were obtained. Because of the improved heat transfer characteristics of this configuration, further studies will be made.

Swage Compaction. A swaging machine was installed, and a major effort will be made to develop swage-compacted fuel rods for superheater applications.

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2. REACTOR MECHANICAL STUDIES

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2.1 VESSELS AND STRUCTURES

2.1.1 Vessel Shell and Nozzles

2.1.1.1 Design Status. Final design of the vessel shell is complete except for any minor modifications that may be dictated by results of the full scale closure test. The vessel and internal structures are shown in Figure 2.1.

2.1.1.2 Fabrication. All major vessel components were received with the exception of the vessel neck and lower hemispherical head. Delivery of these two items is expected by mid-May.

The vessel shell is being fabricated at the West Allis Works of Allis-Chalmers. A large automatic welding machine is being modified and qualified to overlay the stainless steel cladding on the plate material for the cylindrical portion of the vessel shell. Another smaller machine is already qualified for welding the main vessel seams. A minimum of two men per shift are qualified for all hand welding operations.

2.1.1.3 Radiographic Tests. Radiographic tests of a full-scale model of the recirculation inlet nozzle were completed. Ceramic inclusions measuring 0.062 and 0.125 inch in diameter by 5/16 inch length were placed at various positions and orientations in the nozzle walls and connection welds. The welds were radio-

Figure 2.1 Vertical Section of Controlled Recirculation Boiling Reactor with Nuclear Superheater (A-C Dwg. 43-001-999).

graphed using an Allis-Chalmers betatron. Adequately clear radiographs were obtained for all inclusions placed in the model. The presence of a 10-degree taper at the nozzle weld preparation facilitated radiographic examination. The test indicates that radiographic inspection techniques with the betatron are entirely adequate to ensure the integrity of the recirculation inlet nozzle welding in the reactor vessel.

2.1.2 Bolted Flange Closure

The conceptual design of the bolted-flange closure is complete pending results of the full-scale closure tests.

The full-scale closure test vessel is being fabricated and is 90 per cent complete. The lower test head was completed and installed on foundation at Greendale Laboratories. Piping, auxiliary equipment, and instruments are being installed. The control rod drive mounting sleeves are being welded into the upper head and should be completed in mid-April. Testing is scheduled to begin in mid-May.

2.1.3 Reactor-Vessel/Shield-Pool Connection

The reactor vessel will be located in a circular compartment under a pool of shielding water. The vessel flange will be connected to the bottom of the shield pool by an expansion joint assembly that permits vertical movement resulting from thermal

expansion of the pressure vessel. The expansion joint assembly also prevents shield pool water from entering the reactor compartment.

The vessel closure is covered by metallic insulation and a stainless steel shell. The annular flange region of the closure is covered by a separate stainless steel sealing ring. This ring with two rubber gaskets will form seals with the stainless steel insulation shell and the expansion joint assembly, and will prevent shield pool water from entering the insulation space.

Tests are being made of various gasket configurations to determine the optimum design, to determine if hydrostatic pressure is sufficient to maintain the seals, and to determine if differential thermal expansion and manufacturing and assembly tolerances will cause improper gasket seating.

2.1.4 Moisture Separators and Support Shelf

2.1.4.1 Design Status. Two commercial moisture separators, a centrifugal type and a mesh type, are being considered for use in the reactor. The type that is more efficient will be determined by tests in a 20-inch diameter column that will also be used for moisture de-entrainment tests.

2.1.4.2 Moisture De-Entrainment Tests. The 20-inch diameter test column is being fabricated and should be completed in late May. Further investigations of the natural separation of entrained moisture will be made in this relatively large diameter vessel and a diametrical correction factor will be

Figure 2.2 Volume fraction of steam in a non-circulating two-phase mixture in a vertical 4-inch diameter pipe. The density of the mixture was measured well below the two-phase interface. (A-C Dwg. 43-301-491).

calculated. Evaluation of results should give a reasonably accurate indication of the quality of steam produced in large boiling water reactors.

During moisture de-entrainment tests with the 4-inch diameter test section⁽¹⁰⁾, the volume fraction of steam at various steam release rates was determined by measuring the fluid density in the test column well below the two-phase interface. Results are given in Figure 2.2. From this data the following equations were derived:

$$\phi = 0.142 p^{0.167} v^{0.64}, \text{ when } v < 1 \text{ fps} \quad (2.1)$$

and

$$\phi = 0.115 p^{0.2} v^{0.42}, \text{ when } v > 1 \text{ fps} \quad (2.2)$$

10. J. Wilson and M. McDermott, Pathfinder Atomic Power Plant, Final Report, Moisture De-Entrainment Tests in Two- and Four-Inch Diameter Test Sections, Allis-Chalmers Report ACNP-5921, 1959

Figure 2.3 Reactor grid plate. (A-C Dwg. 43-500-739).

where,

Φ = volume fraction of steam,

P = system pressure, psia

v = velocity of steam above the two phase interface, fps.

The equations will predict the volume fraction of steam for the 4-inch diameter test section. Whether the diameter of the vessel affects the applicability of these equations is not known, since measurements were made in the 4-inch diameter section only.

2.1.5 Boiler Core Structure

2.1.5.1 Boiler Core Grid Plate. Conceptual Design. The boiler core grid plate supports and locates the boiler fuel elements, and supports the boiler core baffle. It also provides a mounting surface for control rod pedestals that in turn support the control rods in the down position. The reference design for the grid plate for the aluminum-clad core is shown in Figure 2.3. The design for the alternate zirconium-clad core is essentially the same.

The plate is a sandwich-type structure consisting of two annular, perforated plates joined at the periphery by a heavy web and spaced toward the center by 96 cylindrical spacers. The spacers receive the boiler element nozzles. The sixteen control rod pedestals are bolted to the upper side of the assembly. This sandwich-type of structure is used to reduce the

thermal stresses in the plate. To further reduce thermal stresses, coolant is circulated within the sandwiched plating to cool their inner surfaces and the nozzle inserts.

Final designs of both the reference and alternate grid plate is essentially complete, and material for fabrication has been received.

Grid Plate Deflection Test. A grid-plate deflection test was conducted to determine the maximum deflection that might be expected when the plate is fully loaded. Only a relatively small deflection can be tolerated, since any deflection tends to produce tilting of boiler elements. In addition, the grid plate is a complicated structure in which stresses are difficult to calculate. Semi-empirical determinations of deflection and stress are possible provided the shear and flexural rigidity of the grid structure are known. Since only an estimate could be supplied for the grid plate, and since a more accurate determination was desired, a quarter-scale model was built. This enabled a direct measurement of stresses and deflection in the model, and these can, by similitude, be readily converted to Pathfinder stresses and deflections.

Test Apparatus. The quarter-scale model was made of Plexiglas II. A comparison of significant characteristics of both the model and Pathfinder grid plate are given in Table 2.1.

Table 2.1 Comparison of Model and Reactor Grid Plate

Parameters	Reactor	Model
Material	304L SS	Plexiglas II
Operating Temperature, F	489	72
Relative linear dimension, in.	1	0.250
Total load, lb.	33,600	Variable
Modulus of elasticity, psi	25.5×10^6	0.480×10^6
Poisson's ratio	0.317	0.35

The test apparatus is shown in Figure 2.4. To ensure uniform support along the periphery of the model, the model was seated in the support stand ring with plastic steel. A uniformly distributed load of 1860 pounds was imposed on the grid plate with an inflatable diaphragm. Dial indicators were mounted beneath the model at the locations shown in Figure 2.5. Corresponding reactor grid plate deflections were calculated by the equation,

$$Y = \frac{1}{N} \frac{P}{P_m} \frac{E_m}{E} Y_m \quad (2.3)$$

where,

N - Length scale (ratio of length on prototype to a corresponding length on the model)

P_m - Load applied to model, psi

Figure 2.4 Grid-Plate Load test rig. (A-C Dwg. 43-401-069).

P - Reactor load

E - Modulus of elasticity of stainless steel

E_m - Modulus of elasticity of Plexiglas II

Y - Deflection in reactor grid plate, inches

Y_m - Deflection in model grid plate, inches

The model deflections measured and the reactor grid-plate deflections calculated by Eq. (2.3) are given in Table 2.2. Results indicate a deflection of 0.0029 inches in the reactor, which should present no problem. Since recirculation flow will tend to reduce the total load, the value is conservative.

Figure 2.5 Position of dial indicators on model grid plate.
(A-C Dwg. 43-024-174)

Table 2.2
Deflections in Model and Reactor Grid Plates

Location	Model Deflection (inch)	Predicted Reactor Deflection (inch)
1	0.0263	0.00223
2	0.0240	0.00204
3	0.0006	0.00005
4	0.0247	0.00209
5	0.0066	0.00056
6	0.0136	0.00115
7	0.0146	0.00124
8	0.0386	0.00287

2.1.6 Superheater Core Structure

2.1.6.1 Superheater Moderator Flow Test. The lower two tube sheets in the Pathfinder Superheater will include orifices that control the flow of moderator. Since the precise flow requirements are not as yet known, a general test was conducted to determine the hydraulic characteristics of the

geometry. The objective was to develop sufficient data to design the orificing at such time as the flow requirements are established.

Test Apparatus. The test apparatus used in these experiments is shown in Figure 2.5. The flow channel was formed with 4 pipes 1.050 inch o.d. disposed on a square pitch spacing of $1\frac{3}{16}$ inch. Due to fabrication tolerances and deformation due to welds, the pitch spacing average 5 inches downstream from the tube sheet orifice measured 1.167×1.179 inch; the gaps between the tubes at the centerline were 0.117 and 0.129 inch. The apparatus was made in two sections: one 12-inch long section upstream and one 24-inch long section downstream of the tube sheet. Both ends of the sections were closed by welding the tubes to the flanges. The tube sheet with a sharp-edged orifice was inserted between the two sections.

The test section was installed in a hydraulic test loop constructed of 2-inch standard pipe. A circulating pump rated at 80 gpm, 250-ft head was used. The flow rate was varied by means of a by-pass valve directly downstream from the pump and a throttle valve downstream from the test section. The flow rate was measured by reading the pressure drop across a Meriam precision orifice plate on a differential mercury manometer.

Figure 2.6 Pathfinder superheater orifice flow test assembly.
(A-C Dwg. 43-301-453 and A-C Photo 206870).

Static pressure taps, suitably located for measuring the pressure drop across the tube sheet were installed on the test section; the differential pressure between one set of pressure taps located 4 inches upstream from the tube sheet and two sets of pressure taps located 1 and 5 inches downstream from the tube sheet were read simultaneously on two differential mercury manometers.

A pressure gage was mounted between the throttle valve and the test section, thus indicating the pressure in the test section downstream from the tube sheet.

Testing Method. The pressure drop and discharge coefficient across the tube sheets of 1/2- and 1-inch thickness were determined experimentally for sharp-edged orifices of .266-, .3125-, .4375-, and .500-inch diameters. Each plate thickness and each orifice diameter were tested at various flow rates up to the maximum differential pressure that could be read on the differential manometers. Data was thus obtained for a wide range of Reynolds number and beta ratios. For each geometrical configuration, two differential pressure readings were made simultaneously between the pressure taps located 4 inches upstream and 1 and 5 inches downstream from the tube sheet. The differential pressures between the pressure taps located 4 inches upstream and the pressure taps located 11 and 17 inches downstream were obtained only to insure that the full energy regain was completed at the pressure tap located 5 inches downstream from the tube sheet.

Calculations: The hydraulic diameter, De , was based on a finite channel, taking account of the wall effect, and was calculated by the equation,

$$De = 4 \left[\frac{Pitch^2 - \pi/4 d_i^2}{\pi d_i + 4(Pitch - d_i)} \right] \quad (2.4)$$

or in its actual value, as follows:

$$De = 4 \left[\frac{1.167 \times 1.179 - \pi/4 \times 1.05^2}{\pi \times 1.05 + 2(1.117 + .129)} \right] = .54$$

The Reynolds numbers,

$$Re = \frac{De \times V \times \rho}{\mu} \quad (2.5)$$

were based on the average velocity and hydraulic diameter, De , of the channel.

The permanent pressure loss across the tube sheet sharp-edged orifice can be expressed as follows:

$$\Delta H = \frac{KV^2}{2g} \quad (2.6)$$

where,

V - Fluid velocity through the orifice, fps

K - Resistance coefficient

g - Acceleration due to gravity, 32.2 ft/sec^2

In order to have Eq. (2.6) in the form,

$$H = \frac{KQ^2}{d_o^4} \quad (2.7)$$

a conversion factor, m, was introduced as follows:

$$\Delta H = \frac{mKQ^2}{d_o^4} \quad (2.8)$$

where,

$m = .261 \times 10^{-2}$ conversion factor (when Q, d_o , H are expressed as follows:

K = Resistance coefficient

Q = Flow, gpm

d_o = Orifice diameter, inches

ΔH = Manometer deflection, feet of water.

The discharge coefficient, C, for the tube sheet sharp-edged orifice can be expressed by the following flow formula:

$$Q = C A_o \sqrt{2g R \left(\frac{S_o^{-1}}{S_l} \right)} = C.F.S. \quad (2.9)$$

where,

A_o = Orifice area

R = Gage deflection in feet of mercury

S_o = Mercury density

S_l = Water density

g = Acceleration due to gravity, 32.2 ft/sec²

or C can be expressed as follows:

$$C = \frac{n \cdot Q}{d_o^2 \sqrt{\Delta H}} \quad (2.10)$$

where,

n = .053 conversion factor when Q, d_o , and H are as below

Q = Flow, gpm

d_o = Orifice diameter, inches

ΔH = Manometer deflection, feet of water

Results. Test results are presented as a function of flow in Figures 2.7 through 2.16. Two curves were obtained for each configuration; the upper curve is the pressure loss measured within the Vena Contracta 1 inch from the discharge edge of the orifice; the lower curve, which represents the permanent pressure loss, was obtained by measuring the pressure loss 5 inches from the discharge edge of the orifice, where the full energy regain was realized.

The equations for these curves can be expressed as follows:

$$\Delta H = f(Q^2) \quad (2.11)$$

For a given sized orifice, the coefficient, k , was found to be constant for the flow range tested, and the equation,

$$\Delta H = k Q^2 \quad (2.12)$$

was therefore applicable. (See Figures 2.7 through 2.16). The influence of the orifice length (plate thickness) on the pressure loss is shown for the 1-inch thick plate in Figures 2.7 through 2.12. The influence of a 1/16-inch x 41-degree chamfer on the inlet side of a 3/8-inch diameter orifice and 1-inch thick plate is shown in Figure 2.10.

The back pressure in the test section was regulated by the discharge throttling valve; during the test, this pressure was kept between 15 and 20 psi. The change in permanent pressure loss measured 5 inches from the orifice discharge edge was within the test accuracy; however, for the same conditions, when the

pressure loss was taken within the Vena Contracta, 1 inch from the orifice discharge edge, noticeable changes were observed when the differential pressure was over 60 feet of water.

Conclusions. It should be noted that the diametral ratio, beta, does not fully describe the discharge channel flow geometry. To describe completely the channel flow geometry for loss through a tube sheet orifice, the diametral ratio, beta, should be varied with two parameters, viz., the hydraulic diameter, D_h , and the orifice diameter, d_o . Since the main interest was the Pathfinder flow channel, only the orifice diameter was varied in this experiment.

The differences between the test results of the 1- and 1/2-inch thick plate should be attributed to test errors instead of the influence of the plate thickness. The orifice for the 2 plates were drilled separately. Therefore, an error in the discharging area, as well as a small percentage change in the head, affects the discharge.

The importance of the upstream edge is demonstrated by Figures 2.9 and 2.10. By changing the sharp-edge to a 1/16-inch x 40 degree beveled edge, the flow was increased by 18.5 per cent.

Figure 2.7 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice 0.266-inch in diameter through a 1 inch thick plate. The Reynolds number varied from 11,000 to 44,000. (A-C Dwg. 43-201-948).

Figure 2.8 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice 0.3125-inch in diameter through a 1-inch thick plate. Reynolds number varied from 11,000 to 71,000. (A-C Dwg. 43-201-949).

Figure 2.9 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice 0.376 inch in diameter through a 1-inch thick plate. The Reynolds number varied from 11,000 to 104,000. (A-C Dwg. 43-201-950).

Figure 2.10 Orifice flow test. The permanent pressure loss was measured for an orifice 0.376 inch in diameter through a 1-inch thick plate. The upstream edge was 1/16-inch $\times$ 40 degree beveled. (A-C Dwg. 43-201-951)

Figure 2.11 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice with a 0.440-inch diameter through a 1-inch thick plate. The Reynolds number varied from 22,000 to 135,000. (A-C Dwg. 43-201-952).

Figure 2.12 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice 0.504-inch diameter through a 1-inch thick plate. The Reynolds number varied from 22,000 to 130,000. (A-C Dwg. 43-201-953).

Figure 2.13 Orifice flow test. Permanent pressure loss was measured for a sharp-edged orifice of 0.3125-inch diameter through a 1/2-inch thick plate. (A-C Dwg. 43-201-954).

Figure 2.14 Orifice flow test. Permanent pressure loss was measured for a sharp-edged orifice with a 0.376-inch diameter through a 1/2-inch thick plate. (A-C Dwg. 43-201-955).

Figure 2.15 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice with a 0.4375-inch diameter through a 1/2-inch thick plate. (A-C Dwg. 43-201-956).

Figure 2.16 Orifice flow test. The permanent pressure loss was measured for a sharp-edged orifice with a 0.500-inch diameter through a 1/2-inch thick plate. (A-C Dwg. 43-201-957).

2.1.6.2 Superheater Tube Weld Test. Introduction. Various joint designs were proposed for welding Type 304 superheater container tubes to the Type 304 upper tube sheet in the Pathfinder reactor. The weld that joins these two components must be leak-proof. The Allis-Chalmers Research Laboratories conducted an investigation to determine the most satisfactory of the proposed designs.

Materials and Welding Procedure. The tube material was Type 304 stainless steel, one inch diameter and 0.020-inch wall thickness. The tube sheet material was type 304 stainless steel, 3/4 inch thick. Weld preparations in the tube sheet are shown in Figure 2.17. The tubes were inserted into the tube sheet (the fit-up is shown in Figure 2.17) and were fused to the tube sheet with a Heliarc torch. The surface appearances of the six designs after welding are shown in Figures 2.18. Argon was used for the shielding gas. No filler metal was used except for the second pass of design 4, in which case a 1/16-inch diameter type 308 filler rod was used. A welding current of 30 amperes was used for design 1 and 40 amperes was used for the remaining designs. Design 1 and 6 had the best weldability characteristics in the weld tests.

Physical Tests. Two methods were used to measure the distortion of the tube sheet caused by the heat of welding and the shrinkage of the weld metal (Figure 2.19). As shown in Figure 2.19a, a straight edge was positioned, so that the sagitta of the

Figure 2.17 Sketches of various tube to tube-sheet joint designs.
(Dwg. 51-433)

Design 1

Design 4

Design 2

Design 5

Design 3

Design 6

Figure 2.18 Surface appearance of joint designs after welding.
(Photo 2486-A)

(a)

(b)

Figure 2.19 Two methods of measuring distortion of tube sheet.
(Dwg. SI-434)

Figure 2.20
Method used
to determine
strength of weld.
Dwg. SI-435

Table 2.3

DISTORTION MEASUREMENTS

Weld Joint Design No. (See Fig. 2.17)	Distortion	
	Sagitta - "A" in.	Deflection - "B" in.
1	0.014	0.042
2	0.033	0.098
3	0.035	0.090
4	0.053	0.158
5	0.032	0.098
6	0.016	0.058

arc could be measured as shown. In Figure 2.19b, a straight edge was positioned as a cantilever so that the deflection could be measured. Feeler gages were used for the measurements given in Table 2.3. The least distortion occurred when joint designs 1 and 6 were used.

An attempt was made to determine the breaking strength of the weld. Using the set-up shown in Figure 2.20, a load was applied to the mandrel to force the tube out of the tube sheet. Because of the short tube length, the tubes could not be gripped to pull them out of the sheet. All tubes collapsed; the welds did not fail.

Metallographic Examination. Sections were cut from each of the welded joints and were mounted for metallographic examination. All fused areas had a microstructure of austenite plus pools of ferrite (about 10 per cent), as may be seen in Figure 2.21. The samples were then macro-etched and low power photomicrographs were taken of each joint design (Figures 2.22).

In design 1, the depth of fusion appeared to be inadequate. The control of penetration could be assured by using a tube-to-tube sheet welding gun. In design 4, an excessive overhang may be seen, which is undesirable. The additional filler added in making weld design 4 was not required.

Figure 2.21 Austenite-plus-ferrite structure which was typical of all fused joints between the tube and tube sheet.

Magnification: 1000X
Etchant: Oxalic acid, electrolytic
(Photo 2846-4-B)

Summary and Conclusions. Of the six joint designs proposed for Heliarc welding of type 304 stainless steel tubes to a tube sheet, design 1 and 6 (Figure 2.17) were preferred, because of ease in welding and low distortion. Tests to determine the strength of the welds indicate the necessary strength can be developed. The fused areas had a microstructure of austenite plus about 10 per cent ferrite, which is desirable to preclude hot cracking. Some welds had a shallow fusion, which could be corrected by using a semi-automatic welding gun.

Figure 2.22 Sections through tube to sheet welds.
Magnification 12.5 X., Etchant: 10 per cent oxalic acid.
(Photos 2846-C)

Future Plans. A small test apparatus to determine the effect of thermal cycling and pressure upon the tube-to-tube sheet welds is being constructed. This set-up will also be used to determine the feasibility of a rolled joint between inner and outer container

tube. This joint will be made by welding a spacer to the inner tube and then rolling the assembly into the outer tube and tube sheet. A condenser tube expander will be used to roll the joint. The joint will be disassembled with a conventional condenser tube puller.

2.1.6.3. Superheater Support Tube Vibration Test. A test apparatus was designed and is being fabricated. It includes a lattice representing a typical section of the prototype superheater; it consists of a vertical rod bundle of 16 tubes 192 inches long, 1.080 inch o.d. x 0.020-inch wall thickness disposed on a square pitch spacing, rigidly attached at both ends to the grid plates, and guided between by 4 perforated tube sheets simply supported by the enclosing box.

The vibration of the tubes will be measured as water flow through the section is varied. Measurements will be made by strain gages suitably mounted on the inside surface of the tubes.

2.2 RECIRCULATION SYSTEM

2.2.1 Recirculation Pumps

The prototype pump casing arrived at the West Allis Shops and is shown in Figure 2.23. The 25,000-lb, stainless steel casting passed the initial hydrostatic pressure test and is being finish machined. Some of the scheduling on the hot loop test of the prototype pump was revised due to late arrival of the casing and impeller castings. Foundry orders were placed on schedule for the major castings on the production pumps.

A preliminary specification was written for the gland-seal booster pumps that supply seal water to the recirculation pumps. Flow and head requirements for the seal water pumps will be verified by a cold seal test now being run.

The PWR hot test loop is being modified to accommodate the Pathfinder prototype pump. One end of the test loop will be rigidly anchored to simulate Pathfinder plant conditions.

The prototype pump support stand is designed to accommodate both a rigid mounting, and the trunnion mounting that will be used in the Pathfinder plant. A part of the hot loop test will allow observation of the trunnion mounting under pipe expansion due to warmup. Since one end of the hot test loop is anchored, movements of the pump due to pipe expansion should be quite

Figure 2.23 Prototype recirculation pump casing.
(A-C Photo 207023)

similar to those that will occur in the Pathfinder plant.

Design of the prototype trunnion mounting is virtually complete, and preparations are being made for its purchase.

2.2.2 Recirculation Loop Valves

The butterfly valve was tested to determine the effect of air content on the incipient cavitation point and to verify previous test data. To determine the effect of air content, three tests were run with water that had been de-aerated by boiling the water in the loop. Boiling was observed through the portholes in the stand pipe. The water was held at this temperature for three hours to drive off the free and dissolved air.

Since almost 5 hours was required to bring the loop temperature to boiling, no test could be run at that time. The water was allowed to cool overnight. The following day, the temperature was raised to 180 F, and incipient cavitation data was taken. A sample of the test water was taken and found to contain no visible air. Another sample was taken and heated from room temperature to boiling. The first bubbles were driven off at 180 F, which indicates that the water contained very little dissolved air, even after cooling to room temperature.

If the water was saturated with air at 180 F, it would have had dissolved air content of 6.4 cc/liter, whereas saturated water at 70 F has an air content of 18 cc/liter. It is evident from this, that a large portion of the dissolved air was removed due to the temperature of the tests.

Because the water was boiled before the test, and the loop was closed except for a small area exposed to air at the surface of the water in the standpipe, it is believed that the dissolved air was below the saturation joint, but no chemical analysis was made. For a comparison, additional tests were made with aerated water.

The results are shown in Table 2.4. A comparison between the tests with aerated and deaerated water is made in Figure 2.24 by averaging the sigma values. The results show that cavitation occurs sooner (at larger sigma values) with water that contains visible air.

Figure 2.24 Butterfly valve test. Sigma at incipient cavitation for various valve angles with aerated and deaerated water. Test conditions were as follows: Temperature - 180 F, Downstream pressure - 44 feet. (A-C Dwg. 43-024-186)

Table 2.4 Sigma at Incipient Cavitation for Various Valve Angles

Valve Angle (degrees)	Test Number				Test Number			
	1	2	3	Average	4	5	6	Average
90	4.40	3.34	3.83	3.86	4.46	4.89	3.88	4.41
85	3.67	3.16	3.66	3.50	4.03	3.83	3.36	3.74
80	3.41	2.97	3.64	3.34	3.54	3.85	2.94	3.44
75	2.92	2.97	3.39	3.10	3.90	3.65	2.70	3.42
70	2.94	2.58	3.14	2.88	4.56	3.37	2.92	3.62
65	2.81	2.91	3.06	2.92	3.36	3.49	2.90	3.25
60	2.45	2.88	2.68	2.67	3.57	2.77	2.80	3.05
55	2.29	2.39	2.48	2.39	2.85	2.75	2.43	2.68
50	1.86	1.93	2.14	1.98	2.19	2.25	2.49	2.31
47	1.69	1.75	2.06	1.83	2.04	2.16	2.01	2.07
45	1.54	1.62	2.00	1.72	1.93	2.08	1.90	1.97
43	1.49	1.56	1.79	1.61	1.87	2.11	2.14	2.04
41	1.43	1.57	1.70	1.57	1.78	1.89	1.98	1.88
35	1.35	1.35	1.55	1.42	1.70	1.65	2.00	1.78

The values of σ obtained from these tests were about 30 per cent lower than corresponding σ values obtained in previous tests. A large drop in the coefficient of discharge was also noticed.

When the loop was disassembled, the upstream edge of the plastic pipe was found to be peeled back, so that it projected into the flow stream. This condition is shown in Figure 2.25. Since the large drop in coefficient of discharge was noted after the water had been boiled, it is reasonable to assume that the deformation was caused by overheating, and that the projection was not present during previous tests. Because this projection affected the pressure drop measured across the valve, the test data given in Table 2.4 will not be used to determine the throttling limit (incipient cavitation point) of the valve. The data does indicate, however, that the critical values of σ are affected by air in the water. Since the earlier tests were conducted with water containing air and since reactor water will be highly deaerated, the σ values determined in the earlier tests are conservative.

2.3 Steam Separators

An inclined perforated baffle plate was fabricated and tested in the steam separator loop. The baffle plate as fabricated and mounted in the standpipe is shown in Figure 2.26. The perforations

Figure 2.25 Butterfly valve. The upstream edge of the plastic pipe peeled back so that it projected in the flow stream. This affected the pressure drop across the valve and caused lower sigma values. (A-C Photo 206867)

Figure 2.26 Inclined baffle plate. An inclined perforated baffle plate was fabricated (left) and installed (right) in the steam separator test loop. (NP Photos 4A-0-2 and 4A-0-10).

in the plate are one-inch diameter holes with 30 per cent of the plate area removed. The inclined baffle was designed to simulate the inclined flow deflection plate that will be installed 2 feet above the boiler core outlet in the reactor vessel.

The relative amount of air flow from the separator and the efficiency of the separator were measured in test runs at various recirculation and air flow rates with the baffle plate placed in the stand pipe 2 and 4 feet above the baffle sleeve outlet and at angles of 45 and 60 degrees with respect to the horizontal. Test results are given in Tables 2.5 through 2.8. Results of tests without the baffle plate are given in Table 2.9. Although preliminary analysis of the data shows that the inclined baffle plate has little effect on separator efficiency at reactor conditions, a thorough evaluation is being made.

Table 2.5 Separator Efficiency with Baffle Inclined at 60°, Centered 2 feet above Baffle Sleeve Outlet with Static Water Level at 4 feet above Baffle Sleeve Outlet.

Total Free Air Supplied (CFM)	Water Flow Rate (gpm)	Percentage of air into Separator	Percentage of Air Carried-Under by Volume of Mixture Flow from Separator
77	1330*	64.3	0.048
78	1015	21.7	negligible
78	710	11.2	negligible
150	1330*	76.1	0.144
149	1000	28.6	0.033
150	715	8.0	negligible

*Corresponds to reactor recirculation flow rate.

Table 2.6 Separator Efficiency with Baffle Inclined at 45°. (Other Conditions are same as in Table 2.5)

Total Free Air Supplied (cfm)	Water Flow Rate (gpm)	Percentage of Air into Separator	Percentage of Air Carried-Under by Volume of Mixture Flow from Separator
76	1340	79.2	0.105
78	1025	47.4	0.032
78	700	27.9	negligible
152	1330*	84.8	0.136
153	1000	83.0	0.051
149	710	91.0	0.028

* Corresponds to reactor recirculation flow rate.

Table 2.7 Separator Efficiency with Baffle Inclined at 60°, Centered 4 Feet above Baffle Sleeve Outlet with Static Water Level 4 Feet above Baffle Sleeve Outlet.

Total Free Air Supplied (cfm)	Water Flow Rate (gpm)	Percentage of Air into Separator	Percentage of Air Carried-Under by Volume of Mixture Flow from Separator
77	1340	70.9	0.095
78	1000	36.6	0.025
77	710	11.1	0.020
150	1330*	69.4	0.139
150	1000	35.3	0.048
148	710	14.0	0.029

* Corresponds to reactor recirculation flow rate.

Table 2.8 Separator Efficiency with Baffle Inclined at 45° (Other conditions same as Table 2.7)

Total Free Air Supplied (cfm)	Water Flow Rate (gpm)	Percentage of Air into Separator	Percentage of Air Carried-Under by Volume of Mixture Flow from Separator
80	1340	78.5	0.071
77	1010	35.8	0.033
77	700	13.4	0.023
149	1330*	77.5	0.132
142	1000	31.0	0.052
147	700	9.7	0.027

* Corresponds to reactor recirculation flow rate.

Table 2.9 Separator Efficiency without Baffle and with Static Water Level 4 Feet above Baffle Sleeve Outlet

Total Free Air Supplied (cfm)	Water Flow Rate (gpm)	Percentage of Supply Air into Separator	Percentage of Air Carried-Under by Volume of Mixture Flow from Separator
77	1330*	71.0	0.140
77	1000	31.0	0.070
77	700	8.0	0.050
149	1330*	69.0	0.218
150	1000	30.0	0.094
150	700	12.5	0.080

* Corresponds to reactor recirculation flow rate.

Testing of the separator with a No. 6 mesh screen over the baffle sleeve outlet was completed for static water levels of 2 and 4 feet. The per cent by volume of air carried under as a function of recirculation rate is plotted in Figure 2.27. The curves show that carry-under increases with a drop in water level and increasing recirculation rate. The highest carry-under observed during the test was 0.313 per cent at reactor operating conditions and with a 2-foot static water level. Carry-under for a 4-foot static water level was 0.219 per cent for the same flow rate. Data taken during this test will be evaluated with data from a test presently being conducted with a No. 10 mesh screen over the baffle sleeve outlet. This evaluation will show the effect of bubble size on carry-under and separator efficiency.

Figure 2.27 Percentage carry-under vs recirculating water flow with 145-cfm air injected. (A-C Dwg. 43-024-159).

2.4 CONTROL RODS, GUIDE TUBES, AND CONTROL ROD DRIVES

2.4.1 Control Rods

2.4.1.1 Superheater Control Rods. Alternate design for the superheater control rods is an assembly consisting of 13 poison fingers ganged together in a cruciform array. The poison fingers will have a 3/4-inch diameter and will be connected to a cross-shaped support by stainless steel tubing. Final selection of poison material was not made as yet, and other design and manufacturing details are being established.

Enough information is available to begin detailing and manufacture of a testing apparatus to test the control rod. A

preliminary layout of the proposed test apparatus is shown in Figure 2.28. The test is being designed to check the following; 1) control rod drive latching ease, alignment, and torque; 2) effect of scrambling impact on the rod; 3) effect of guide tube galling, bowing, and binding on rod operation; and 4) underwater handling and manipulation of rod and adjacent fuel elements during refueling.

2.4.1.2 Boiler Control Rods. Development work on the boiler control rod is essentially complete except for the corrosion testing of boron steel in steam. These tests are now being made.

2.4.2 Guide Tubes

The Zircaloy-2 guide tube for hot control rod drive tests was manufactured and is now undergoing autoclave corrosion tests. This guide tube should be delivered during the next quarter.

Representative mill samples of 304 stainless steel with 2 per cent natural boron were received, and mechanical tests were performed. The results of these tests are given in Table 2.9. An examination of the microstructure (Figure 2.30) showed the boron to be in a much finer and homogeneous dispersion than previously tested samples.

Table 2.9 Mechanical Properties of 2-percent Boron 304 Stainless Steel

Sample No.	Temperature (C)	Tensile Strength (psi)	Yield Strength (psi)	Elongation (percent)	R.A. (percent)
1	70	94,750	52,600	13	18.6
2	70	95,200	46,600	12	15.4
3	70	91,200	43,000	8	12.9

Table 2.10 Mechanical Properties of Transversally Welded, Boron-Stainless Steel Samples

Sample No.	Temperature (C)	Tensile Strength (psi)	Elongation (Percent)	R.A. (percent)
1	70	79,500	3	3.8
2	500	69,300	2	4.3
3	1000	63,600	3	1.9

Figure 2.29 Comparison of microstructure of boron stainless steel samples. Magnification: 150 X (NP Photos 16A-0-1 and 16A-0-2).

Mechanical tests of welded boron stainless steel samples were concluded. Samples were welded axially and transversally to the direction of pull. After machining, it was noted that the test section of the axially welded samples was nearly all weld metal. Only the mechanical properties of the transversally welded samples are therefore reported in Table 2.10. All samples tested fractured immediately adjacent to the weld area.

2.4.3 Control Rod Drives

2.4.3.1 Prototype Control Rod Drive. Status. The prototype control rod drive is being manufactured at the Allis-Chalmers West Allis Works. A clean room area is being used for cleaning, checking, and assembling the drive mechanism prior to shipment to Greendale. The position indicating system was received, and the limit switch operation was checked. The bench performance check-out appears satisfactory.

The drive rack snapped in two during the final straightening operation. This rack is machined from 17-4PH stainless steel. A preliminary examination of the failed parts did not disclose the cause of the break. A dye-penetrant inspection of the rack surface failed to disclose any other cracks or defects.

Since the rack failed at the uppermost circumferential gear tooth it was decided to repair the rack by welding a length

of 304 stainless steel to the 17-4 PH material, and then machine the gear teeth in the 304 stainless steel. Galling tendencies do not appear to be a problem with this repair since the upper teeth contact the pinion gear only when the rack is removed from the drive mechanism. The repair is expected to be completed in April.

The hot test control panel is almost complete, and the hot test vessel is about 90 per cent complete. All heaters, instruments, and gages are on hand.

Dashpot. The dashpot for the prototype drive was tested to evaluate its operation. This testing was deemed necessary because it would have been very difficult to measure the internal pressure or stopping action of the dashpot when it is inside the drive housing; and failure of the dashpot would result in damage to both the control rod drive and the control rod. In the assembled control rod drive, there is no method of determining if the dashpot returns to its raised position after a scram. It was therefore essential that the dashpot be scrambled a sufficient number of times to insure that no galling or sticking occurs which would cause the dashpot to stick in the down position.

In the assembled control rod drive, the dashpot is located inside the drive housing and above the drive pinion.

The control rods scam downward and the dashpot has a six-inch working stroke in which to arrest the motion of the control rods before they bottom on the boiler core grid plate. The dashpot also permits the rack an additional one and one half inch of travel for latching and unlatching.

The dashpot was designed to give an approximately uniform deceleration to the control rod during the dashpot's six-inch stroke. The parabolic taper required for a uniform deceleration was approximated with two straight-line tapers for ease of fabrication. In the design it was assumed that the rod had an initial velocity of 15.8 feet per second and the total weight to be stopped was 223 pounds. If the deceleration were uniform, this would have resulted in a deceleration rate of 7.7 g's and a uniform internal pressure of 1104 psi.

The dashpot, as machined, had a slightly smaller orifice at the initial part of the stroke, but was within reasonable agreement with the design. Figure 2.30 shows 1) the calculated ideal taper, 2) the approximated taper, 3) the taper as it was fabricated, and 4) a modified taper that was made as a result of this test.

Test Program. The dashpot was mounted in a vertical position and a weight approximately equal to the weight that the dashpot is expected to decelerate was dropped from various heights into the dashpot. The test apparatus is shown in Figure 2.31.

Figure 2.30 Dashpot design and modification. (A-C Dwg. 43-024-204)

The following instrumentation was used to measure the action of the dashpot: 1) a high-speed motion picture camera operated at about 1000 frames per second; 2) a Baldwin SR-4 pressure cell calibrated from 0 to 2000 psi; 3) strain gages; and 4) a Tektronic oscilloscope with camera.

The Baldwin pressure cell was connected to the pressure chamber in the dashpot through a 1/16-inch hole drilled in the dashpot base. This provided a direct measure of the pressure inside the dashpot. Strain gages were tangentially adhered to the lower region of the dashpot casing to measure the strain developed in the casing. A strain gage was also placed on the upper region

Figure 2.31 Control-rod dashpot test set-up. (A-C Photo 206444).

of the ram to measure stresses in the ram. The high speed camera was directed at the position indicator and gave a picture of the stopping action of the dashpot. By analyzing the films it was possible to plot the position of the weight as a function of time while it was being decelerated by the dashpot.

A block weighing 227 pounds was dropped by a quick-release device onto the dashpot. The weight was raised with a lift truck. The velocity with which the weight hit the dashpot was calculated as that for free fall from the release height and verified by analyzing the motion pictures. The velocity and position of the weight as it was being decelerated by the dashpot was also found from the motion pictures. The camera was equipped with a device that produced a mark on the film every $1/120$ of a second and permitted accurate measurement of camera speed.

The vertical movement of the scope sweep was used to measure the output from the strain gage or pressure cell, and the sweep on the scope was triggered by an increase in pressure in the system. It would thus produce a picture of the strain or pressure developed inside the dashpot while it was decelerating the falling weight. The dashpot stroke was about one inch longer in the test than it would be in the drive, but the piston was prevented from bottoming by blocks placed on the lower guide support to stop the weight. This additional travel made it possible to analyze the velocity with which the control rod would be expected

to hit the grid plate with greater accuracy than if the motion were stopped at the expected point of impact.

The 227-pound weight was dropped from various heights, and the measurements as described above were taken. Before each drop the dashpot was manually filled with demineralized water. The weight was later increased to 303 pounds and again dropped from various heights, but no motion pictures were taken of the stopping action. This procedure permitted analyzing the dashpot action with a number of impact velocities and also showed its general characteristics with a larger falling mass.

Test Results. The dashpot was examined and measured before it was tested. This preliminary examination showed that the fit between the ram and the outer casing was very close. When the ram was in the up-position, metal-to-metal contact would cause the ram to stick. This was corrected by scraping the inside of the upper region of the casing.

Use of the rubber O-ring on the ram caused enough friction to prevent the ram from returning from the down position. The groove for the O-ring was therefore modified to take a metal piston ring. All sharp corners on the ram were also removed at this time. With these modifications, the ram moved freely and seemed to have a minimum of sliding friction.

Initial testing showed that the pressures and strains developed in the dashpot shortly after impact were about three times

their average values. These high pressures were minimized by modifying the dashpot taper as shown in curve 4 of Figure 2.30. Figure 2.32 shows the pressure developed in the dashpot before the taper was modified and Figure 2.33 shows the strains. The vertical calibration in the pressure measurement was not linear, but the initial pressure peaks are apparent.

Figure 2.34 shows the improvement in these pressure peaks after the taper was modified. Figure 2.35 shows the almost total elimination of pressure peaks. This was done by placing one-half inch of Neoprene rubber between the weight and the ram.

Figure 2.36 shows a strain curve taken with the 303 -pound weight and one taken with the 227-pound weight. This change in weight caused the maximum pressure developed to occur as the dashpot reached the change in taper near the end of its travel. No motion pictures were made to determine the velocity of this larger weight, but calculations from the pressure curve indicate that the velocity at the grid plate was about 42 in/sec for the strain curve shown. This may be compared to a velocity of 22 in/sec for the 227-pound weight having the same initial velocity.

The dashpot was cycled a total of 100 times. The dashpot operating with a cast-iron piston ring returned to the raised position after the first 48 cycles but stuck in the down position on the 49th drop. The dashpot was then disassembled and examined. Examination of the upper guide on the ram showed a number of shallow axial scratches. There were no scratches on the lower ram guide.

Figure 2.32 Pressure in dashpot prior to modification. Vertical scale is not linear and horizontal sweep is 20 msec/cm. 227-lb weight was dropped 46 in.

Figure 2.35 Effect of one half inch Neoprene buffer on dashpot. 227-lb weight was dropped 5 ft 1 in. (left) and 5 ft 11 in. (right) with (left) and without (right) a neoprene buffer. The scale of dashpot strain is as follows:
Vertical scale: 1 cm = 150 uin/in
= 550 psi
Horizontal scale: 1 cm = 1 msec

Figure 2.33 Strain in dashpot prior to modifying taper. Vertical scale is not accurate, but the high initial strains are apparent. 227-lb weight was dropped 66 in. and sweep time was 5 msec/cm

Figure 2.36 Comparison of strains developed with 303-lb weight (left) and 227-lb weight (right). The weight was dropped from 5 ft 0 in. The scale is as follows:
Vertical scale: $3\frac{1}{2}$ cm = 600 uin/in.
= 655 psi
Horizontal scale: 1 cm = 10 msec

Figure 2.34 Strain in dashpot after modifying taper. 227-lb weight was dropped 50 in. Note change in magnitude of initial strains after modifying the taper. The scale is as follows:
Vertical, 1 cm = 150 uin/in. = 550 psi
Horizontal, sweep time = 5 msec/cm

Figure 2.37 Corresponding strains developed in the dashpot casing (left) and the ram (right). 227-lb weight was dropped 50 in. in both. Sweep time was 1 msec/cm.

Examination of the inside of the outer casing showed a small metal deposit. The deposit showed no signs of rust and was assumed to be chrome plate or stainless steel. This deposit was removed by scraping and rubbing with emery cloth, and the reassembled dashpot showed no signs of sticking or binding.

After six additional drops, the dashpot again stuck in the down position. It was again disassembled and found to have another metal deposit and more scratches on the upper guide for the ram. The following modifications were then made to the dashpot: 1) the inside of the outer cylinder was honed out to remove any metal deposits; 2) a 1/32-inch taper was put on the lower end of the cylinder to make insertion of the ram and piston ring easier; 3) the outside diameter of the upper guide on the ram was turned to 2.495 inches and a slight taper was added to its upper region. This completely removed all scratches and any indication of binding or friction.

The dashpot was tested again after these modifications and showed no signs of binding after 30 additional cycles.

The dashpot was also tested with the rubber O-ring instead of the cast-iron piston ring. The rubber ring caused considerable friction between the piston and the outer casing, and the piston would not always return to the raised position. When it did return, it did so slowly.

Figure 2.37 shows the strain in the upper region of the ram and the strain in the cylinder at the same time intervals. The

sequence of occurrence is 1) the weight hits the ram and causes the high strain in the ram, 2) a pressure wave travels through the ram and returns causing the weight and the ram to separate (there is no strain in the ram while they are separated; by conservation of momentum, the separation velocity of the ram can be calculated to be nearly 1.75 times that of the falling weight) 3) the ram's high initial velocity causes a pressure surge that quickly slows down the ram; 4) the weight overtakes the ram and the process is repeated until the ram and weight reach the same speed. One way to eliminate the separation action would be to use a material with a low coefficient of restitution between the weight and ram. The peaks at the start of dashpot action are minimized by the change in taper and dashpot action is satisfactory, but the peaks are not eliminated.

Figure 2.38 shows a typical displacement curve plotted from the motion picture. By scaling the slope of this curve, it was possible to obtain values for velocities at various instants during deceleration. The velocity of the ram at various instants is plotted as a function of the velocity of the weight on contacting the dashpot in Figure 2.39 for the lower limits. These curves show that the weight is being decelerated as it passes the grid plate datum plane.

Figure 2.38 Typical plot of movement of falling weight in dashpot test. (A-C Dwg. 43-024-169).

Figure 2.39 Velocity of the control rod near boiler core grid plate as determined by dashpot test. (A-C Dwg. 43-024-203).

Conclusions. Figure 2.39 shows the velocity with which the control rod will collide with the grid plate, and its velocity at various positions above and below the grid plate. From this curve it is seen that the velocity is well arrested at the grid plate datum plane. If a control rod 1/4 inch too long is used, however, it will hit with nearly double the velocity of a standard length control rod.

This situation can be remedied by changing the tapered portion of the dashpot to 5-1/2 inches rather than 6 inches, and allowing the weight to coast the remaining half inch. This would protect the control rod from hitting the grid plate before the rod's motion is properly arrested, particularly if thermal expansion, manufacturing tolerances, and other factors cause an effective increase in the rod length.

The cause of the dashpot sticking in the down position in both instances was a piece of chrome or stainless steel becoming lodged between the guide and the outer casing. Some machining had been done in the shop on the outer casing after it was flash-chromed. This operation left the chrome edge rough, so that it is possible that a piece of chrome got into the dashpot, or that a piece of stainless steel got into the dashpot from some other source. The reduction in the size of the upper guide of the ram appears to have reduced binding and made the dashpot operate with less friction. It is recommended that the clearance

between the upper ram guide and outer housing be reduced to help eliminate the cause of friction, however.

Results indicate that a metallic piston ring was the most satisfactory seal for the ram. The use of a rubber ring was an effective seal, but the dashpot did not satisfactorily return to a raised position after a scram. There is also a question of irradiation damage to rubber O-rings. When no piston ring was used and the initial velocity of the weight was 230 in/sec, the velocity at the grid plate datum plane was about 2 in/sec faster than that with the ring, and 4 in/sec faster a quarter of an inch below the datum plane.

The piston ring could probably be satisfactorily eliminated if the taper were adjusted to allow for a small amount of leakage past the guide and the outer casing.

The piston ring used in the test was made of cast iron. This would not be satisfactory in hot tests of the prototype drive, and a brass ring was recommended by the ring manufacturers. This type of ring was ordered but did not arrive in time for this test.

The maximum stress developed in the dashpot casing with the 227-pound weight was measured to be 18,800 psi. This is well within the elastic range of the material, and there is no danger of plastic deformation. With the 303-pound weight, the maximum measured stress was 20,300 psi. This again was within the elastic

range of the material, and no permanent deformation should be expected. The velocity of the rod near the grid plate with the larger weight is probably excessive, and it would be necessary to modify the dashpot to take the additional weight.

The cause of the pressure peaks as the ram starts its stroke were first thought to be shock waves traveling through the fluid. However, this frequency was not in agreement with any calculated frequency of the system. Further investigation showed that they were caused by separation of the ram and the falling weight.

The results of the test indicate the following performance of the dashpot when a completely withdrawn control rod is slammed.

Mass to be stopped	223 pounds
Calculated velocity of mass when contacting dashpot	190 in/sec
Dashpot stroke	6 inches
Maximum deceleration	10.4 g
Average deceleration	7.7 g
Maximum internal pressure	1450 psi
Control rod velocity when contacting grid plate	122 in/sec*
Average internal pressure	1100 psi
Dashpot stroke time	.061 sec

*The taper will be modified to reduce this velocity to about 10 in/sec.

Motor, Gear Box, and Clutch. The single-phase induction gear motor was tested in air and in transformer oil and operated satisfactorily. It has been decided to test a similar three phase induction motor since the single-phase electrical circuit has become complicated.

A servo motor and double worm gear reducer were also tested, but shortly after testing began, all of the teeth on one of the pinions in the gear reducer sheared off, and no further testing was done. During this test, it became apparent that the unit would have excessive coast in the down direction and would require a brake.

The clutch slipped in oil under slight overloads, which may be due to the flexible test set-up. The next larger size clutch has been ordered as an alternate.

Some results of the single phase gear motor test in oil are as follows:

	<u>No Load</u> (ms)	<u>240-inch/pound Load</u> (ms)
starting time, up	100	150
starting time down	90	90
stopping time, up	90	50
stopping time, down	95	120
reversing time, up-down	155	115
reversing time, down-up	190	260
clutch release time	-	14

The output rpm, a 105.8:1 reduction, is 16.2 rpm up and 17.0 rpm down under no load. Under load of 240 inch-pound, the speed is 14.2 up and 17.0 rpm down. The maximum lifting torque is 280 inch-pounds.

2.4.4 Oscillator Rod

A literature survey was conducted to ascertain the design parameters and to study the existing oscillator rod designs. Design specifications for the oscillator rod were prepared, and an analysis of the various possible types of mechanisms was started. The analysis is proceeding on the basis that it will be located wholly within the control rod guide channel. Other arrangements, however, are also being investigated. For example, a rotating type of rod is being evaluated although it will not fit in the control rod channel, and four special fuel elements will be required. One design based on a series of geared discs and another based on a cam type reciprocator are being studied in detail. Figure 2.40 shows the layout of the first.

No definite conclusion was reached on either as yet.

Figure 2.40 Poison generator: (A-C Dwg. 43-002-120)

3. NUCLEAR ANALYSIS

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3.1 REACTOR PHYSICS (STATICS)

3.1.1 Alternate Zirconium-Clad Core Nuclear Constants

The same nuclear model that was used in calculations of the reference aluminum-clad boiler core was used to determine constants for the zirconium-clad core. Few group fast constants were calculated with the Muft IV code and fit to an experimentally determined Pre^{28} . Thermal flux distributions from transport-theory cell calculations were used to weight material number densities in calculating thermal constants with the Sofocate code.

A comparison of nuclear parameters between the zirconium- and aluminum-clad cores can be seen in Table 3.1. This comparison is for cores with the same fuel-to-water ratio. The fuel lattices are essentially the same with the zirconium-clad core lattice having a slightly increased fuel pin size, with a

Table 3.1 Comparison of Nuclear Constants for Aluminum- and Zirconium-Clad Cores

Core Lattice	fuel load kg^{25}	<u>Water</u> Fuel (UO_2)	Pre^{28}	τ_{th}	ηf_2	k_{eff}
9 x 9 Al-Clad	65.4	2.219	.842	44.2	1.381	1.187
9 x 9 Zr-Clad	69.8	2.219	.842	41.7	1.432	1.240
8 x 8 Al-Clad	51.6	3.215	.885	40.6	1.299	1.156
8 x 8 Zr-Clad	55.2	3.215	.885	38.7	1.350	1.206

0.350-inch o.d. uranium dioxide pellet rather than a 0.338-inch o.d. pellet. The core multiplication was calculated for the cold condition from the following relationship:

$$k_{eff} = \frac{1}{1 + \tau B^2} \left[\eta f_{fast} + \frac{\eta f_{thermal} \rho}{1 + L^2 B^2} \right] \quad (3.1)$$

3.1.2 Flux and Power Distribution

Because of the similarity between the reference aluminum core and zirconium core in fuel lattice and fuel-to-water ratio, the flux and power distribution is not significantly different between them.

3.1.3 Core Burnup

Reactivity changes with fuel depletion and isotopic buildup during core operation were calculated by a two-dimensional perturbation method. Fuel depletion and buildup of heavy isotopes and fission products were calculated with the Allis-Chalmers BAD code (See ACNP 5915) assuming constant power distribution. This code solves for the isotopic number densities as a function of irradiation time and calculates the changes in absorption and fission cross sections.

Reactivity changes due to perturbations in different core zones are appropriately weighted to determine the total reactivity

change. The contributions by element and region were determined for the two cases studied.

The core is divided into 30 zones, each with a constant power fraction. The isotopic density equations are solved, and cross-section changes determined for each zone. Fission produced poisoning is calculated separately.

The total reactivity change for a time step is expressed as,

$$\frac{\Delta K}{K} = \frac{\int_R \delta(\epsilon \nu \Sigma_f) \phi_1^+ \phi_2^+ dV - \int_R \delta \Sigma_a \phi_2^+ \phi_2^+ dV}{\int_R \nu \Sigma_f^0 \phi_1^+ \phi_2^+ dV} \quad (3.2)$$

where the integrations are performed over the 30 zones. The spatial modes of the flux distributions are assumed to remain constant over a time step.

Using the technique outlined above and the flux and power distributions obtained from a two-group, two-dimensional diffusion calculation (RZ geometry, PDQ code) the change in reactivity as a function of time was calculated.

The expected change in reactivity for the highly enriched superheater with the zirconium boiler core is given in Table 3.2.

From this preliminary analysis, the initial burnable poison requirements can be specified to keep the reactivity swing within the specified limits. The results from these calculations agree closely to those performed by the use of the Candle-2 burnup code for the reference aluminum-clad core design.

The reactivity change for a slightly enriched (3.38 per cent) superheater with a zirconium-clad core is given in Table 3.3.

Table 3.2 Reactivity Change for Highly Enriched Superheater and Zirconium-Clad Boiler (Per Cent $\Delta k/k$)

Reason for Reactivity Change	Reactor Operating Time in Days at 0.8 Load Factor		
	75	225	450
Fission Product Buildup (Including Sm^{149} build-up, Xe and Sm Changes)	-2.06	-2.42	-2.79
U^{235} Depletion and U^{236} Addition	-3.18	-8.86	-16.00
Heavy Element (Pu Iso- topes) Buildup	+2.06	+4.47	+5.67
Total Core Changes	-2.55	-5.46	-11.41

Table 3.3 Reactivity Change for Low-Enrichment Superheater and Zirconium-Clad Boiler (Per Cent $\Delta k/k$)

Reason for Reactivity Change	Reactor Operating Time in Days at 0.8 Load Factor		
	75	225	450
Fission Product Buildup (Including Sm^{149} build-up + Xe & Sm Changes)	-2.62	-3.33	-4.00
U^{235} Depletion and U^{236} Addition	-8.63	-15.31	-20.00
Heavy Element (Pu Iso- topes) Buildup	+5.60	+7.07	+6.84
Total Core Changes	-4.95	-10.16	-16.07

3.1.4 Nuclear Constants

In order to establish techniques, models, and constants applicable to the study of the Pathfinder core, detailed studies and calculations are underway for the slab cores and experiments described in Sec. 3.3. Final criticality and temperature coefficient analyses were completed for the unvoided 9×9 and 8×8 fuel-lattice cores. A preliminary critical calculation of the 9×9 core was reported in the last progress report (ACNP 6001).

The general method of analysis consists of two-dimensional (PDQ code), two-group, XY, diffusion-theory calculations with the control rod channels included explicitly. The axial buckling was obtained from a one-dimensional diffusion theory code (Wanda).

Fast constants were obtained from the Muft code with the axial water gaps fast-flux weighted from results of diffusion calculations. The resonance escape probability and Doppler broadening were included from the results of experimental data.

Thermal constants were obtained from the Sofocate code with the fuel pins and axial water gaps flux-weighted from results of transport calculations.

The data are summarized in Tables 3.4 and 3.5. The agreement between theory and experiment appears good considering that the reactivity swing corresponding to a temperature change from 20 to 82 C is extremely small. In addition, recent data indicates that the magnitude of the Doppler effect (approximately -0.2 per cent $\Delta k/k$ from 20 to 82 C), which was included in the calculation,

Table 3.4 Absolute Criticality

Temperature (C)	Calculated K_{eff}		Experimental K_{eff}	
	9 x 9	8 x 8	9 x 9	8 x 8
20	.9996	1.0081	1.0025	1.0025
50	.9975			
82	.9949	1.0054		

Table 3.5 Reactivity Swing from 20 to 82 C

Calculated Per Cent $\Delta k/k$		Experimental Per Cent $\Delta k/k$	
9 x 9	8 x 8	9 x 9	8 x 8
-.47	-.27	-.33	-.1 (approx.)

is too large by about 30 per cent. A correction would further improve the agreement between theory and experiment. This effect and minor refinement in the flux weighting technique will be investigated during the next quarter.

The remaining experiments described in Sec. 3.3, viz., the voided cores, the rod-in experiment, and the low enrichment boiler core, are being analyzed.

3.1.5 Fission-Gas Release from Uranium Dioxide

An I.B.M. 704 code is being written to evaluate fission-gas release from cylindrical, uranium dioxide, fuel under neutron irradiation. Irradiation time, heat generation, and fuel density will be variable input terms. The thermal conductivity of uranium dioxide as a function of temperature will also be a code variable.

The fission gas release model (WAPD 183 and CRDC 721) considers the diffusion from small spherical particles to be the controlling rate mechanism. The marked temperature dependence of the diffusion process requires the evaluation of the fission-gas release from a series of small cylindrical shells of the fuel. A summing process, which flux weights the individual sections for fission-gas production, will provide the total fractional release for the element. In addition to the fractional fission-gas release, the code output will include the fuel-rod, center-line temperature profile.

3.1.6 Shielding

Dose points on the operating floor around the high pressure turbine were calculated with local shielding placed next to the entering steam lines. This local shielding will adequately

shield the entering steam lines. The dose due to these lines is 1 mr/hr.

A new shielding program 04-4 was obtained for the I.B.M. 704 having 32,768 words of magnetic core memory. This shielding program is actually composed of three separate shielding programs: 1) 04-1 using rectangular source geometry; 2) 04-2 using cylindrical source geometry; 3) 04-3 using a series of point sources, so that any shape source can be described. Due to the larger I.B.M.-704 memory unit that this code uses, it can handle many more materials, regions, and gamma-ray energy groups than the other two shielding codes that were described in previous reports. The 04-4 shielding program will be used on future problems that warrant the use of a larger program.

Dose rates in water from an empty Zircaloy-2 fuel box, a spent boiler element, and a spent superheater element with its stainless steel extension, were calculated. The dose rates were calculated for the case where the elements are in a vertical position in the fuel storage pool, and the dose points are directly above. For a dose of 5 mr/hr; 6 feet, 10 feet, and 11.5 feet are required above the top of the Zircaloy-2 fuel box, the spent boiler element, and the spent superheater-element extension, respectively. After 100 days decay, 4.5 feet, 4.5 feet, and 11 feet are required, respectively, for the same dose.

3.2 REACTOR AND SYSTEM DYNAMICS

3.2.1 Recirculation System Hydrodynamic Analysis

The Pathfinder hydrodynamic analysis of the recirculation system was completed. A second order transfer function was derived to describe the oscillation phenomenon of the water flowing through and above the core. The transient response of the recirculation flow is described by non-linear differential equations that can accommodate virtually any arbitrary input disturbance. The necessary equations are presently being prepared for inclusion in the Pathfinder Simulator.

Since the analysis includes the inertial effects of the recirculating pumps and motor rotors as well as the speed-vs-torque characteristic of the induction motors, the output information will include the following: 1) individual pump rotor speed, 2) individual loop flow rate, and 3) total reactor flow rate. The necessary inputs for the analog simulation are: 1) synchronous speed of induction motors, 2) applied voltage to induction motors, 3) individual butterfly valve positions, and 4) average quality of steam at core exit.

Figure 3.1 Recirculation loop transport lags simulation schemes.
 The rods are withdrawn at a rate of 5 cents/second for 10 seconds.
 (A-C Dwg. 43-024-163)

3.2.2 Methods of Simulating Transport Lags

The effects of improving the simulation with better representation of transport lags inherent in the system differential equations are shown in Figure 3.1. The comparison of transient response to a typical disturbance of reactivity insertion at 5 cents/second for 10 seconds is shown. It is seen that a 1st order Padé approximation of the transport lag yields a somewhat higher peak power and superheater temperature than the more convenient, but less accurate, simple lag simulation. Similar comparisons using 2nd order Padé approximations and variable lags will be made during the next quarter. The value of using the Padé simulations will be determined in terms of their associated extra equipment. A more detailed description of Padé expressions for transport lags will be available in an interim report on the simulator, which is being prepared.

3.2.3 Transfer Function Study

The entire reactor, turbine, and pressure control system were subjected to a sinusoidal input. The output of $\delta n/n_0$ was recorded and a $\delta u/n_0 \delta k$ closed loop transfer function was obtained. The closed loop transfer function was plotted with per-cent δK . Both P_1 (steam-dome) and P_2 (steam-line) control were tried

Figure 3.2 Pathfinder simulation frequency response for P_1 regulation and P_2 regulation. (A-C Dwg. 43-024-177).

separately. Both curves in Figure 3.2 show that a low-frequency resonance occurs at about 0.2 rad/sec. It has been shown that the superheater pressure drop directly affects this resonance peak. The inclusion of this flow resistance between the pressure sensing point and the turbine reduces the system, low-frequency damping coefficient. A more judicious choice of controller constants could reduce the magnitude of this resonance somewhat for P_1 control. For frequencies above 1 rad/sec, both P_1 and P_2 responses become identical. The high-frequency resonance at 17 rad/sec is caused chiefly by the fuel thermal time constants.

Generally, it is seen that the reactor system is stable with both types of control. The system has a greater margin of stability, however, with P_2 control.

3.2.3 Response to Changes in Major Reactor Parameters

A program was initiated to determine the effect of changing major reactor parameters that would result in important variations in system dynamic response. The effect of one half and double the accepted void-reactivity coefficient magnitude is shown in Figure 3.3. The effect of major boiler-fuel time constants of 2 and 10 seconds is compared with the accepted time of 4 seconds in Figure 3.4. The Doppler coefficient is varied from one half to double its accepted value in Figure 3.5.

Figure 3.3 Effect of variation of void-reactivity coefficient. Control rods are withdrawn at a rate of 5 cents/second for 10 seconds. P2 regulation is used. (A-C Dwg. 43-024-164).

Figure 3.4 Effect of variation of boiler fuel major time constant. Control rods are withdrawn at a rate of 5 cents per second. P₂ regulation is used. (A-C Dwg. 43-024-160).

Figure 3.5 Effect of variation of doppler coefficient. Control rods are withdrawn at a rate of 5 cents per second for 10 seconds. P_2 regulation is used. (A-C Dwg. 43-024-161).

3.2.4 Recirculation Limitations

Controlling Pathfinder with recirculating water flow rate, either manually or automatically, must be limited to a maximum sustained change and a maximum rate of change. These maxima must insure a maximum peak of less than 115 per cent full power and a maximum reactivity insertion equivalent to withdrawing a control rod at 5 cents/second. Simulation results indicate that the more stringent limitation is the 5-cent/second reactivity insertion, which corresponds to a 30-second period. The maximum reactivity insertion rate corresponds to a rate of change of 50 lb/sec^2 (45.5 gpm/sec). The maximum sustained change of 600 lb/sec (545 gpm) corresponds to a maximum 115 per cent full-power limitation. These results assume steam-line pressure regulation.

3.2.5 Superheater Pressure Drop Study

Assumptions were made that, though not credible, would result in a maximum superheater pressure drop. The assumptions were as follows: 1) the reactor is at full power and steam flow to the turbine continues, 2) a step insertion of $1 \Delta K$ occurs, 3) the pressure regulation system does not operate, and 4) the low-pressure and high-pressure relief valves open simultaneously at the high-pressure set point. Under these conditions, the computer recorded a maximum pressure drop of about 225 psi, which the superheater structure can withstand.

3.2.6 Pile Oscillator

Design of a pile oscillator was started; the mechanical design is described in section 2.4.

It is thought that the presence of boiling noise in a boiling water reactor will be quite important in a reactor stability study. A discussion was held with J. A. Thie of Argonne National Laboratory to determine noise characteristics of a boiling water reactor. It was decided that a noise spectrum analysis, an alternate to the pile oscillation technique, should not be used to determine reactor stability because of the unpredictability of the noise characteristics.

It was therefore decided that a pile oscillator approach would be used. The oscillator will have sufficient reactivity to overcome a large assumed noise level at low reactor power levels. It is estimated that a total available δK of 10 cents would overcome a noise level that is 10 per cent of the operating level at low power levels.

The post-construction experiments to be carried out with the pile oscillator will be formulated during the next quarter.

3.2.7 Time-Dependent Neutron Flux Distribution

The problem of time-dependent neutron flux distributions was studied further and a simple one-energy group diffusion model with void feedback was formulated. A one dimensional

case was programmed for the analog computer and various disturbances were impressed on the void distribution in a small (50 cm width) reactor.

No flux tilting was noticed because of the close nuclear coupling in the small core. Further studies will be carried out on a large (300 cm width) core and an analysis will be performed to show the nuclear coupling effects in a large boiling water reactor model that might induce flux tilting.

3.3 CRITICAL EXPERIMENTS

3.3.1 Void Experiments in a 9 x 9 Fuel Rod Lattice

The measurement of the critical configuration, temperature coefficient of reactivity, and flux distribution in the 9 x 9 lattice of 1.8 per cent enriched boiler fuel was discussed in the last quarterly progress report (ACNP-6001). During this quarter, experimentation continued on the 9 x 9 lattice, in order to measure the reactivity worth of moderator voids.

The void measurement was accomplished by starting with a just-critical unvoided slab configuration of 18-1/3 elements of 1.8 per cent enrichment and 9 x 9 lattice array. Void simulators

(foamed polyurethane plastic with a hypalon coating to reduce water absorption) were introduced into the lattice to give a water displacement equivalent to an 18-percent void content. In order to remain critical, the core size was increased until a $4 \times 6\text{-}2/9$ voided element configuration was reached. This core had an excess of 0.25 per cent $\Delta k/k$. The calculated reactivity difference between the unvoided and voided slab is 3.2 per cent $\Delta k/k$. The average worth of the void content in this small slab core is then about 0.18 per cent $\Delta k/k$ per percent void. It should be emphasized that this void coefficient was measured for a small slab core for the purpose of verifying theoretical predictions and is not representative of void coefficients of the full Pathfinder core.

The critical core configuration for the voided 9×9 lattice is shown in Figure 3.6. Also outlined in this figure are the core locations at which flux traverses were taken to

Figure 3.6 Plan of $4 \times 6\text{-}2/9$ element core used during 9×9 lattice activation studies with 18.5 per cent voided core. (A-C Dwg. 43-024-130).

Figure 3.7 East-West Radial flux plot of $4 \times 6\text{-}2/9$ element core with 9×9 lattice. Active core was fully loaded with 20 percent void simulators. The approximate void fraction was 18.5 percent. Bare 1/2-inch Mn-Cu wires were used to monitor flux.
(A-C Dwg. 43-201-905)

Figure 3.8 Axial flux plot of $4 \times 6\text{-}2/9$ element core with a 9×9 lattice. The active core was fully loaded with 20-percent void simulators. The approximate void fraction was 18.5 per cent. Bare 1/2-inch Mn-Cu wires were used to monitor the flux. (A-C Dwg. 43-201-906).

provide information on flux peaking at water gaps and reflector savings. The measured flux peaks are compared with transport calculations to verify the adequacy of the calculations and models used. The reflector savings are determined by fitting the measured flux points away from boundaries to a cosine curve by use of the COFIT code. Radial and axial flux plots are shown in Figures 3.7 and 3.8, respectively.

3.3.2 Critical Slab with an 8 x 8 Fuel Array

The variation of axial fuel loading that is referenced for the Pathfinder core consists of a 9 x 9 fuel array in the lower core section and an 8 x 8 fuel array in the upper core section. Prior to experimenting with this type of core, measurements were made on cores consisting only of 9 x 9 fuel and 8 x 8 fuel. The 8 x 8 slab configuration of 1.8 per cent enriched fuel was constructed in a manner similar to that described in Sec. 3.3.1; i.e. starting with a critical configuration, progressive substitutions of fuel boxes containing an 8 x 8 fuel array were made until a full critical slab was achieved. The loading of the critical configuration (See Figure 3.9) is summarized in Table 3.6 and compared with the 9 x 9 fuel array core.

Figure 3.9 Critical core cross section. All elements are fully loaded in 8 x 8 array except as noted. (A-C Dwg. 43-024-062)

Table 3.6 Comparison of 8 x 8 and 9 x 9 Fuel-Array Cores

Fuel Array in Element	Core Size Elements	Total Fuel- Pin Loading	Total U-235 (kgs)	Excess Reactivity per cent $\Delta k/k$
8 x 8	24.50	6,272	25.9	0.06
9 x 9	18.33	5,940	24.5	0 (approx.)

3.3.3 Temperature Coefficient of Reactivity

Measurements of the temperature coefficient for the 8 x 8 slab core were made for temperatures of 60 to 175 F. Appreciable scatter of the data points about a zero coefficient was encountered between 60 and 130°F, ranging between $0.3 \times 10^{-5} \Delta k/k/^\circ F$ and $-0.3 \times 10^{-5} \Delta k/k/^\circ F$. At higher temperatures, more consistent data was obtained, corresponding to a temperature coefficient of reactivity of $0.5 \pm 0.1 \times 10^{-5} \Delta k/k/^\circ F$ at 160°F. These measurements were made using the core shown in Figure 3.9.

3.3.4 Void Experiments in an 8 x 8 Lattice

In a manner similar to that described in Sec. 3.3.1, the reactivity worth of 14-percent moderator void in the 8 x 8 fuel array was measured. The unvoided core size of 24.5 elements (Sec. 3.3.2) was increased to 32 elements, 8 wide and 4 deep,

for the voided lattice. This configuration had a reactivity excess of 0.017 per cent $\Delta k/k$. The calculated reactivity difference between the 24.5 element core and the 32 element core is 1.85 per cent $\Delta k/k$, correcting for reactivity excesses of the lattices. The average worth of the void content of this slab is about 0.13 per cent $\Delta k/k$ per percent void.

An attempt was made to determine the temperature coefficient of voided lattices. During this experiment, the reactivity decreased substantially as the moderator was heated, and continued to decrease when the heating was stopped. This decrease in reactivity with an apparently constant core temperature continued over a period of hours. The experiment was stopped until the cause could be determined.

It was postulated that the cause of this effect could be any one or all of the following:

- 1) Poor circulation of the heated water through the elements containing plastic void simulators, causing a substantial lag between measured water temperature and actual core temperature;

- 2) Bubble formation on the rough surface of the void simulators and fuel rods even though the water was agitated;

- 3) A swelling of the simulators with temperature.

At this time, the cause of the measured decrease has not been determined.

3.3.5 Flux Measurements in the 8 x 8 Lattice

Flux traverses were taken in the 8 x 8 lattices for determination of reflector savings, water channel peaking, and cadmium-ratio variation. The location of the traverses through the core are shown in Figure 3.10. The experiments conducted are summarized in the following table and referenced to the graphs on which the results are plotted.

Figure	Core	Traverse	Material Used
3.11	4x6-1/8, unvoided	Axial	Bare 1/2-inch Mn-Cu wires
3.12	4x6-1/8, unvoided	Axial	Bare 1/4-inch U-Al foils
3.13	4x6-1/8, unvoided	Radial, N-S	Bare 1/4-inch U-Al foils
3.14	4x6-1/8, unvoided	Radial, N-S	Cd-Covered 1/4-inch U-Al foils
3.15	4x6-1/8, unvoided	Radial, E-W	Bare 1/4-inch U-Al foils
3.16	4x8, voided	Axial	Bare Mn-Cd wire scanned every 2 inches

Figure 3.10 Cross-section of 4 x 6-1/8 element core used during 8 x 8 lattice activation studies. The position of the flux traverses that were taken are as noted. (A-C Dwg. 43-024-079)

Figure 3.11 Axial flux plot No. 1 for 4 x 6-1/8 element core. Fuel boxes are loaded in an 8 x 8 array. One-half inch Mn-Cu wires were used to monitor flux. (A-C Dwg. 43-201-895).

Figure 3.12 Axial flux plot No. 2 for 4 x 6-1/8 element core. Fuel boxes are loaded in an 8 x 8 array. One-quarter inch U-Al foils were used to monitor flux. (A-C Dwg. 43-201-897).

Figure 3.13 North-South radial flux plot of 4 x 6-1/8 element core with 8 x 8 lattice. One-quarter inch U-Al foils were used to monitor flux. (A-C Dwg. 43-201-889).

Figure 3.14 North-South radial flux plot of 4 x 6-1/8 element core with 8 x 8 lattice. Cadmium-covered 1/4-inch U-Al foils were used to monitor flux. (A-C Dwg. 43-201-893).

Figure 3.15 East-West radial flux plot of $4 \times 6\text{-}1/8$ element core with 8×8 lattice. Bare, $1/4$ -inch U-Al foils were used to monitor flux. (A-C Dwg. 43-201-887).

Figure 3.16 Axial flux plot of 4×8 element core with 8×8 lattice fully loaded with 9×9 , 20-percent void simulators. The approximate void fraction was 15.2 per cent. Continuous bare Mn-Cu wires scanned every 2 cm were used to measure flux. (A-C Dwg. 43-201-894).

3.3.5 Low-Enrichment Slab

As a further check on the ability to theoretically predict critical configurations of boiler fuel, and to determine reactivity variations with boiler fuel enrichment, a critical lattice of 1.585 per cent enriched fuel was built. Each boiler element contained a 9×9 array of 1.8 per cent enriched and natural enrichment fuel rods as shown in Figure 3.17. In each 9×9 cross section, there were 16 natural fuel rods and 65 of 1.8 per cent enrichment.

The critical lattice, constructed in a manner similar to that described in Sec. 3.3.1, consisted of 32 boiler elements arranged in a 4×8 slab (Figure 3.18). The excess reactivity of this lattice was 0.1 per cent $\Delta k/k$.

The calculated reactivity difference between a $3\text{-}6/9 \times 5$ slab of 1.8 per cent enriched fuel and a 4×8 slab of 1.8 per cent enriched fuel is about 5.0 per cent $\Delta k/k$. Since the $3\text{-}6/9 \times 5$ slab of 1.8 per cent enriched fuel was measured and calculated to be just-critical, and the 4×8 slab of 1.585 per cent enriched fuel was similarly found to be supercritical by 0.1 per cent $\Delta k/k$, the reactivity worth of the substitution of 1.8 per cent for 1.585 per cent enriched fuel in a 32 element core is estimated to be 4.9 per cent $\Delta k/k$.

Figure 3.17 Typical element cross section of 9 x 9 lattice with natural enrichment fuel interspersed.
(A-C Dwg. 43-024-136).

Figure 3.18 Critical Core cross-section. All elements fully loaded in 9 x 9 array.
(A-C Dwg. 43-024-135)

A two-group calculation of the reactivity difference between the 1.8 per cent and 1.585 per cent enriched slab showed a 4.7 per cent $\Delta k/k$ difference. This is in fairly good agreement with the above. On a more simplified basis, using the relationship

$\Delta k/k = (1-f) \Delta M/M$, the estimated reactivity difference would be 4.3 per cent $\Delta k/k$.

In quoting the results of these measurements of void and enrichment worth, calculated values here given for the reactivity difference of two slab sizes; reactivity worths of void and enrichment were then deduced from these values. The results were presented in this way because the measured critical sizes were accurately predicted by calculation (See Sec. 3.1), thereby giving confidence in the calculated reactivity difference between two slab sizes, and because of the inability to measure such large

reactivity difference as were encountered in the voided and low-enriched cores.

3.3.6 Boron Rod Worth Measurements

In order to determine the reactivity worth of a boron-stainless steel control rod for comparison with theoretical calculations, a critical slab of 9×9 , 1.8-percent enriched fuel was constructed with a boron rod fully inserted. The size of this slab was found to be $4 \times 6\text{-}5/9$ with 0.18 per cent

$\Delta k/k$ excess as compared to an $5 \times 3\text{-}6/9$ critical slab without a rod. The reactivity held by the rod is then estimated to be 3.7 per cent $\Delta k/k$. A two-dimensional PDQ problem, containing the rod explicitly by boundary conditions, is being set up to check the adequacy of the rod and analysis.

Cross section of the core is shown in Figure 3.19. Flux plots across the blade edge and across the core through a blade are given in Figures 3.20 and 3.21, respectively. The reason for the high scatter of points in the core traverse was not determined.

Figure 3.19 Cross section of $4 \times 6\text{-}5/9$ element core used during 9×9 lattice activation studies of boron stainless steel control rod.

(A-C Dwg. 43-024-189)

Figure 3.20 Flux along blades of fully inserted boron-stainless steel control rod in a $4 \times 6\text{-}5/9$ element core of 9×9 lattice. Bare $1/4$ -inch U-Al foils were used to monitor the flux. (A-C Dwg. 43-024-188)

Figure 3.21 Radial flux plot through the blade of a fully inserted boron stainless steel control rod. The traverse is made through a $4 \times 6\text{-}5/9$ element core with a 9×9 lattice. Bare $1/4$ -inch U-Al foils were used to monitor flux. (A-C Dwg. 43-201-943).

4. CHEMISTRY

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4.1 CORROSION

The test condenser at the site was shut down for the second and final time on February 6, 1960. Inspection showed deep pits and heavy layers of silt-like deposits on all the aluminum alloys tested. It was concluded that aluminum condenser tubes do not perform satisfactorily with the water and water treatment used. Other treatments are available, but a literature search reveals that the treatment chosen was probably one of the best available. Therefore, it appears that aluminum condenser tubes cannot be successfully used in recycled water from the Big Sioux River.

4.2 RECOMBINER, RADIOACTIVE GAS REMOVAL AND STORAGE SYSTEM, ION EXCHANGERS, AND CHEMICAL PROCESSING

4.2.1 Liquid Waste Disposal

Several changes were made in the liquid waste disposal system described in the last quarterly progress report (ACNP-6001). Low solids wastes will flow to a 500 gallon surge tank that serves as a main collection point for these wastes. From this tank, the wastes can be sent to any disposal station: 1) demineralizer, 2) evaporator, 3) decay tank, 4) plant re-use, or 5) discharge to the drainage ditch.

Radioactive liquid wastes will leave the fuel handling building through a pipe leading to the drainage ditch. Discharge to this pipe can originate in one of four ways: 1) continuous drains from sumps, 2) discharge from the fuel storage pool, 3) discharge from the high-purity storage tank, and 4) discharge from the liquid waste disposal system.

Studies indicate that the evaporator can treat regeneration chemicals if any of the resins are to be regenerated.

Revised estimates of liquid wastes are: 12,000 gallons per month of low-solids wastes and 24,000 gallons per month of high solids wastes.

Materials, tank coatings, pump sizes and tank capacities were tentatively selected. The system layout in the fuel handling building was completed.

4.2.2 Reactor Water Purification System

4.2.2.1 System Design, Controls and Instrumentation. A detailed list of design and performance requirements for purification system controls and instruments was completed. The list was incorporated in a complete specification. A similar list was prepared covering control valve requirements. The lists are given in Tables 4.1 and 4.2.

Table 4.1 Controls and Instrumentation Requirements
(Supplement to General Requirements -
Reference ACNP-5924)

Shut Down Pump

a) Motor Load Ammeter	Characteristics to be supplied after motor has been selected
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Superheater Draining Section

a) Valves P-4, P-5, and P-6	
Operator	Motor or Solenoid
Direction of failure P-5 & P-6 P-4	Closed Open
Operating time	Not critical
Operating Positions	Fully open or closed
b) Buffer Zone Pressure Indicator and Alarm	
Indicating Range	0 - 650 psig
Alarm Set Point	105 psig - should be adjustable from 75 to 150 psig

Emergency Isolation Valve P-7

Operator	Motor or Solenoid
Direction of Failure	Closed
Opening time	Not critical
Maximum Closing Time	6 seconds
Operating Positions	Fully open or closed

Blow Down Control Valve P-10

Operator	Pneumatic or motor
Direction of failure	Closed
Opening Time - Full Stroke	30 seconds minimum
Quick closing time	1 second maximum
Manual reset required following valve closure by extreme high level interlock	
Operating Positions	Infinite positioning from closed to fully open
High Flash Tank Pressure at which valve will quick-close	83 psig - <u>81 psig</u> if rupture disk is used for liquid relief.
Extreme High Flash Tank Liquid Level at which valve will quick close	72 in.

Flash Tank Controls

a) Pressure Controller & Indicator

Type of Control	Proportional Plus Reset
Indicating Range	0 - 150 psig
Control Range	0 - 100 psig
Normal Control Set Point	50 psia = 35 psig
Allowable Pressure deviation from set point	± 3.0 psi

b) Pressure Control Valve P-12

Operator	Pneumatic or motor
Direction of Failure	Closed

Valve opening time - full stroke	3 seconds max.
Closing time - full stroke	3 seconds max.
Liquid level at which interlock will close valve	72 in.
c) High Pressure Alarm & Interlock Set Point	83 psig
d) Liquid Level Control & Indicator	
Type of Control	Differential pressure - proportional plus reset
Control Range	0 - 40 in.
Indicating Range	0 - 100 in.
Normal Set Point	22 in.
Allowable Level Deviation from Set Point	± 1 in.
High Level Alarm Set Point	38 in.
Extreme High Level Alarm and Valve Interlock Set Point	72 in.
Normal Operating Pressure	35 psig
Normal Operating Temperature	281 F
Max. Design Pressure	100 psig
Max. Design Temperature	340 F
Control Valve P-46	
Operator	Pneumatic or Motor
Direction of Failure	Closed
Valve Opening Time - Full Stroke	5 seconds or less
Closing Time - Full Stroke	Same as above

Condensate & Circulating Water to
Coolers - Flow Control

Type of Control	Proportional Plus Reset
Control Range	50 - 150 F
Indicator Range	50 - 350 F
Normal Control Set Point	140 F Max.
Allowable Temperature Deviation From Set Point	± 2 F
High Temperature Alarm Set Point	145 F
Valve Operating Time	Not critical

Table 4.2 Control Valve Requirements

Flash Tank Inlet Control Valve, P-10

DESIGN REQUIREMENTS

Fluid Handled	Reactor Water
Fluid Temperature	489 F maximum - design 105°F min.
Design Flow Rate	47,880 lb/hr. 120 GPM
Allowable Pressure Drop (ΔP) at Design Flow Rate-Valve Open Full	2 psi
Nominal Pipe Size In	3"
Nominal Pipe Size Out	3"

SUGGESTED VALVE SIZE AND CHARACTERISTICS

3-inch - single port - plus type with linear characteristics

Normal Cooler -- Condensate Control Valve, P-49

DESIGN REQUIREMENTS

Fluid Handled	Condensate
Fluid Temp. at Design Flow Rate	92°F
Design Flow Rate	29,600 lb./hr. -59.5 gpm
Allowable Pressure Drop (ΔP) at Design Flow Rate - Valve Open Full	7 psi
Nominal Pipe Size In	3 in.
Nominal Pipe Size Out	3 in.

SUGGESTED VALVE SIZE AND CHARACTERISTICS

1-1/2" - single port - plug type with linear characteristic

DISCUSSION

The valve sizing calculations were based on the following pressure drops in the condensate system for a flow rate of 626,264 lb/hr:

Total Piping	8
H ₂ Coolers	14.20
Air Ejector Cond.	2.22
Drain Cooler	2.65
# 11 F.W. Htr.	12.52
# 12 F.W. Htr.	9.50
	<hr/>
	49.09 psi

Pressure drops through the purification system cooler, and piping to and from the cooler, were assumed or calculated as follows:

- For "condensate through cooler" flow rate of 29,600 lb/hr.
- For 3" piping system.

Piping before Cooler	0.81
Piping after Cooler	0.32
Cooler (tube side)	<hr/> 3.00
Total	<hr/> 4.13 psi

The suggested pipe size of 3" was based on a number of considerations. Though 2" piping appeared satisfactory for the design conditions, it was found that with 3" piping, the cooler would operate satisfactorily down to a plant load of approximately 35 MW. With 2" pipe, operation would be limited to a minimum plant load of 43 MW. This factor in itself is not too significant since operation, at reduced plant loads, can always be switched to the shut down cooler if necessary.

It is thought, however, that the estimated ΔP through the parallel portion of the condensate system might be high. If this is the case, the 3" pipe will more closely approach the optimum size than will the 2". In general, the choice of 3" pipe, without adding greatly to costs, will allow for a more liberal margin of error in pressure drop calculation.

Shut Down Cooler - Coolant Control Valve P-50

DESIGN REQUIREMENTS

Fluid Handled	Cooling Tower Water
Fluid Temperature	90°F max.
Design Flow Rate	1530 GPM
Allowable Pressure Drop (ΔP) at Design Flow Rate-Valve Open Full	
Nominal Pipe Size In	6" - 8"
Nominal Pipe Size Out	6" - 8"

SUGGESTED VALVE SIZE AND CHARACTERISTICS

None.

DISCUSSION

This valve was not previously sized by A-C, as were other control valves in the purification system. Only the design flow rate and suggested pipe size possibilities are indicated.

It is thought that Pioneer Service is now in a better position to specify the requirements than A-C.

Flash Tank Liquid Level Control Valve - P-46

DESIGN REQUIREMENTS

Fluid Handled	Filtered & Demineralized Reactor Water
Fluid Temperature	140° maximum operation
Maximum Flow Rate	285 GPM
Design Operating Flow Rate	61.7 GPM
Required Pressure Drop (ΔP) at design Flow Rate - valve in controlling position	P max. = 40 psi P min. = 17 psi
Maximum allowable pressure Drop (ΔP) at maximum Flow Rate - Valve Open Full	P = 55 psi
Nominal Pipe Size In	3"
Nominal Pipe Size Out	3"

SUGGESTED VALVE SIZE AND CHARACTERISTICS

2" - Single port - throttle plus with linear characteristic

This selection based on "Fisher" Single Port Design
"A" Body Throttle Plus Inner Valve

DISCUSSION

When operating normally at the design flow rate, the required pressure drop across the valve will vary as filters and ion exchangers load up and their resistance to flow increases. The specified maximum and minimum ΔP 's for the valve correspond to a completely clean and maximum loaded system, respectively. Within the specified range of ΔP , the valve will be controlling flow (liquid level).

A maximum flow rate of approximately 285 GPM will occur during the superheater draining operation. At this time the flash tank will be flooded and control valve open full. The specified maximum allowable ΔP is consistent with the present expected head - capacity characteristics of the shut down pump, which will be used for draining the superheater

Flash Tank. The flash tank testing study was completed. It showed that in view of the estimated equipment costs necessary to obtain significant test results, pre-installation testing at actual operating conditions was not justified.

A study was made to determine the safety or relief requirements for the flash tank. It was decided that the flash tank would have to be provided with steam and liquid relief. Both systems will dump to the main condenser.

Shut-Down Pump. Final performance and design requirements were established for the shut-down pump. A survey of available pumps established the fact that a suitable unit, designed to withstand full reactor pressure, could be obtained for essentially the same price as one designed for lower pressure. Pressure operated interlocks that protect the shutdown pump from reactor pressure will not be required if the full pressure pump is selected.

4.2.3 Plant Demineralized Water Make-Up System

The Pathfinder demineralized water make-up system is designed to provide water to a storage tank or for direct use. The inter-connecting piping will provide a maximum of flexibility.

The high dissolved solids content of the well water makes treatment before demineralization desirable. A 50-gpm, cold process softener will be used to reduce the dissolved solids from 850 ppm to approximately 550 ppm. This softener will be required to operate from 10 to 65 gpm. To maintain a satisfactory flow at times of low water demand, provision will be included for recycling water through the softener. The softened water will be filtered with Anthrafilt filters before it is demineralized.

The demineralization system will consist of two, 2-bed demineralizers followed by a mixed bed. Each 2-bed demineralizer contains a cation and an anion bed. Each 2-bed demineralizer is

rated at 50 gpm. During periods of demand of less than 25 gpm, all units will be used in series: cation, anion, cation, anion, mixed.

The primary cation and anion units will become exhausted. They will be regenerated and returned to service in the secondary position. Rotating the units in this manner will assure a constant flow with a minimum demineralization load on the mixed bed. For flows of 25 to 50 gpm, the 2-bed demineralizers will be used in parallel. In the event the mixed bed should be inoperative for an extended period, the 2-bed demineralizers operated in series will provide satisfactory water. Water from the 20,000 gallon storage tank may be used directly or recycled through the mixed bed before use.

The two 36-inch diameter cation beds receive filtered water from the 50 gpm cold process softener. At the design flow of 25 gpm, expected life is in excess of 6 hours. The expected flow requirements of 15 gpm will allow approximately 12 hours between regenerations. Backwash and acid dilution water are raw well water; 66°Be concentrated sulfuric acid for regeneration is supplied by a small positive displacement diaphragm pump. The acid is transferred from the large storage tank to a small tank close to the demineralizers, where it is stored until needed. Water from the cold process softener is used for rinsing the resin.

Water from the cation beds goes to the two, 36-inch diameter anion beds. These beds are designed to operate on the same regeneration cycle as the cation beds. Heated backwash and caustic dilution water from a storage tank is used as rinse water to reclaim anion. A positive-displacement piston pump supplies caustic from a small storage tank. The final anion rinse water comes from the cation beds. The last part of this rinse water is diverted to a storage tank where it is heated and used during the next regeneration. The reclaiming of anion rinse water will eliminate the need for the Zeolite softeners and result in less water being required for regeneration.

The mixed bed should have a very small demineralization load and require infrequent regenerations. Backwash and caustic dilution water are supplied from the anion rinse storage tank. Rinse water is supplied by the 2-bed system. There is no provision for saving the anion rinse water and the storage tank is refilled by a line from the 30-inch domestic softener. Acid dilution water and cation rinse water will be raw well water. After the resin has been mixed, water is recycled through the bed to insure optimum performance.

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5. INSTRUMENTATION AND CONTROL

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5.1 INSTRUMENTATION

Specifications are being written for the area monitoring, liquid monitoring, and off-gas radiation monitoring systems.

Pressure control, feedwater control, nuclear instrumentation, and control panel proposals were received and are being reviewed to determine if all engineering aspects of the specifications were met. A series of meetings were held with the vendors to clarify questions on their proposals.

The selection of the supplier will be made after the evaluation is completed.

The proposed method of measuring the liquid level on the wide-range instruments is to use a 1-inch pipe, tapped off the vessel cover, concentric with a 2-inch pipe through the shield pool. The 1-inch pipe will provide sufficient steam volume to limit the rate of condensation back into the steam dome to 5 lb/hr. A quick-disconnect connector will be located at the shield pool surface to be disconnected prior to vessel cover removal. A standard differential pressure cell will be used as the primary instrument in the system.

5.2 PATHFINDER PLANT CONTROL

Various regulation and control schemes were investigated by simulation. In addition to reactor dome pressure (P_1) and steam-line pressure (P_2) regulation, a system regulating by summing pressure errors was attempted. Results are shown in Figure 5.1.

Speed regulation was found to be unsatisfactory without pressure regulation through a by-pass system. This can be reasoned from an overspeed condition when the plant becomes isolated from the rest of the electrical distribution system. The overspeed would cause turbine inlet valves to close and cause an overpressure, assuming no other control of reactor power is provided.

The following comments should be added to those contained in the last quarterly report concerning relative merits of P_1 regulation and P_2 regulation. Because of the superheater pressure drop:

- 1) The closed loop frequency response for P_1 regulation indicates a low frequency resonance (See Sec. 3.2).

- 2) P_1 regulation will require safety valves above the superheater to allow for reasonable settings over a large flow (power) range and a pilot operated relief valve downstream from the superheater to insure flow through the superheater during safety valve relief.

An effort was made to optimize pressure-regulator, proportional, and reset constants. Results of these optimization

Figure 5.1 Pressure regulation schemes. Rods are withdrawn at a rate of 5 cents per second for 10 seconds.

(A-C Dwg. 43-024-162)

trials for a momentary steam flow restriction are shown in Figure 5.2.

Proposals received from various vendors of pressure control systems are being reviewed from a dynamics standpoint. The most critical dynamic response in the pressure control system is the requirement that the by-pass valve open fast enough upon turbine trip, so that pressure deviation will be held below 20 psi. The various proposals were reviewed from this dynamic requirement (plus several others) and technical evaluations and recommendations were made.

Figure 5.2 Comparison of pressure regulator constants with P₂ regulation and with a momentary 33-percent steam-flow constriction.
(A-C Dwg. 43-024-107)

6. PLANT SAFETY ANALYSIS

7. FEASIBILITY STUDIES

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6. PLANT SAFETY ANALYSIS

Reactor safeguards analyses and studies of plant safety are continuing as the plant design progresses. A public hearing on the Pathfinder for the purpose of obtaining a construction permit was held in February and the permit is expected in May.

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7. FEASIBILITY STUDIES

A final report on elimination of vapor closure was submitted by Internuclear Company. The report was entitled, "Internuc-57-IBM-704 Program for Reactor Containment Suppression Analysis," dated March 24, 1960. This report was accepted and minor modifications are being made prior to its being published as part of the Pathfinder R & D program.

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8. STEAM PLANT AND REACTOR AUXILIARY SYSTEMS DESIGN

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8. STEAM PLANT AND REACTOR AUXILIARY SYSTEMS DESIGN

8.1 PATHFINDER REACTOR MASS AND ENERGY BALANCE

The Pathfinder reactor's mass flow rates and energy requirements were brought up to date using recent data. The reactor energy balance, taking the feedwater inlet to the steam-line throttle valve as the system boundaries, assumes steady-state full power operation. Changes in potential and kinetic energy between the inlet and outlet streams were neglected. Consideration was given to external energy losses from the reactor water purification stream, the reactor vessel, control rod drives, and recirculation pumps and piping.

The total reactor power generation is now 199.6 mw with the superheater supplying 42.4 mw and the boiler region contributing 157.2 mw.

The schematic diagram (Figure 8.1) shows the individual energy contributions from both the boiler and superheater regions and the overall mass and energy balance for the Pathfinder reactor system as of April 1, 1960.

8.2 CONTAINMENT VESSEL

Design calculations and drawings were submitted by Pittsburgh-Des Moines Company for approval. The drawings, in general, cover the design of the personnel air lock, emergency air lock, equip-

ENERGY INPUT + ENERGY GENERATION = ENERGY OUTPUT

Figure 8.1 Full power Pathfinder reactor energy balance.
(A-C Dwg. 43-024-129)

ment door, support brackets, electrical connections, etc. In addition, a number of vessel plate and erection drawings were submitted. All drawings and calculations were reviewed and, with few exceptions, approved.

8.3 PROCEDURE FOR TURBINE STARTUP AND PLANT OPERATION

The following procedure is intended to outline the basic steps and equipment used in turbine startup, operation, and shutdown for normal and emergency conditions. This supplements the preliminary reactor startup procedure as given in the quarterly progress report, ACNP-5811, p.25.

Startup Assumptions

- 1) Reactor startup procedure has been followed to obtain design steam outlet conditions.
- 2) Reactor steam outlet pressure is 545 psia.
- 3) Steam temperature is approximately 825 F.
- 4) Turbine is on turning gear.
- 5) Warming steam is being admitted to the turbine.
- 6) Approximately 20,000 lb/hr of steam is being by-passed to the main condenser.
- 7) Turbine by-pass valve is on automatic pressure control (control set at 545 psia).

- 8) Turbine inlet valves are on load limit control.
- 9) Turbine gland seal steam is being supplied from the reactor-steam heated boiler.
- 10) Main condenser is at vacuum.
- 11) Steam jet air ejectors are operating.

Turbine Startup Procedure

- 1) Set speedchanger at 3600 rpm no load speed. Lock inlet valve pressure regulator in 10 mw load position. Increase turbine speed with load limit control from control room.
- 2) Set speedchanger to assume control at 3200 rpm. Set load limit to desired maximum load setting. Increase turbine speed with speedchanger to synchronous speed. Synchronize with speedchanger. Load turbine to 10 mw by increasing speed. Set speedchanger to full open position. Set by-pass valve on manual control. (Steam-line pressure is 545 psia.)
- 3) Set inlet pressure regulator on automatic pressure control.
- 4) Set inlet valve pressure regulator on automatic pressure control.
- 5) Increase plant output by raising reactor power.

Normal Shutdown

- 1) Reduce turbine load by decreasing reactor power.
- 2) At approximately 6 mw, lock the inlet valve pressure regulator in position and set the by-pass valve on automatic pressure control.

- 3) Switch plant auxiliary load to the auxiliary line.
- 4) Reduce turbine load with speedchanger to 0 load as indicated by watt-hour meter.
- 5) Open the main generator breaker at 0 load condition.
- 6) Trip the turbine stop valves. (Steam flow is maintained through the by-pass valve, which controls pressure at 545 psia.)
- 7) Reactor can be shut down or maintained at stand-by conditions under by-pass control.

Emergency Shutdown on Loss of Main Breaker

Increase of turbine speed causes turbine inlet valve roll back, starting at 102 per cent overspeed. Roll back prevents turbine from reaching 110 per cent overspeed trip condition. Speed governor takes over to carry plant load and maintain 100 per cent speed. Excess steam is by-passed to main condenser through by-pass valve, which assumes pressure control and maintains steam-line pressure at 545 psia with a pressure swing of less than 10 psi.

Trip-Out Required by Turbine or Generator Malfunction

All standard trip conditions will result in stop-valve closures. The by-pass valve control is designed to assume pressure control of the steam line and hold the pressure at 545 psia with a pressure swing of less than 20 psi.