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REACTORS—POWER

UNITED STATES ATOMIC ENERGY COMMISSION

AN EVALUATION OF THE CALDER HALL TYPE OF NUCLEAR POWER PLANT

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P. O. BOX 309 CANOGA PARK, CALIFORNIA

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ABSTRACT

Presented herein is the preliminary design of a natural uranium, graphite-moderated, CO_2-cooled reactor and power plant similar to, but larger than, the British Calder Hall plant, with a net electrical output of 130 MWE. The design is conventional, consisting mostly of standard components, the only major uncertainty being the performance of the fuel cladding. A construction cost of \$67 million and a power cost of 17 mils/kwh are estimated for this plant, assuming private utility operation in the United States, which is about three times the cost of power from conventional plants.

Power output and cost for various conditions of temperature and pressure and for increased reactor size are calculated. It is estimated that a similar plant of 740 MWE output would produce power at a cost of 10 mils/kwh. Use of helium as coolant is investigated and found to offer no advantage over CO_2.

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I. INTRODUCTION

by

W. F. Banks

The general objective of this study is the preliminary evaluation of gas-cooled, graphite-moderated, natural uranium power reactors of the Calder Hall type. The study, which was of 3 months' duration, was begun with the expectation that information from the literature about the Calder Hall plant would be augmented and verified by a visit to the plant and by discussion with the British designers. The visit failed to materialize, however, and it was necessary to pursue the study without definite information regarding several critical areas of the design.

It was decided to proceed as follows:

1. Prepare a preliminary design, to be used as the "Reference Design" for the study, of a plant similar to the Calder Hall plant. Changes from the British design would be made, but would be limited to those whose feasibility was apparent without extended analytical or experimental investigation. This reference design, then, would be a plant whose detailed design and construction could begin immediately.
2. Estimate the capital and operating costs of this reference design, and on this basis, estimate the cost of power.
3. Study variations in reactor size and rating and their effect upon plant cost and cost of power produced.

It was assumed from the outset that the fuel element had been thoroughly analyzed and studied experimentally by the British, and that a short study such as this, without experimental work, could not expect to produce an improved design. Accordingly, a fuel element believed to be identical to the Calder Hall element was adopted. A core of convenient size, larger than that of the Calder reactors, was then laid out using this fuel element. A vessel size (57-ft diameter) was determined which would allow the necessary headroom between the tank and the core. A pressure vessel thickness of 4 in. appeared to approach the limits of feasibility for field fabrication, which set the maximum operating pressure at 350 psia. Heat transfer and pressure drop calculations were carried out for various operating pressures to determine the power output, mean outlet temperature, and flow rates. A net electrical output of 65 MWE per reactor, or 130 MWE for the plant, resulted. Design drawings of major plant components were then made and a cost estimate prepared.

The over-all plant layout is shown in Dwg. 1. The plant consists of two reactors, each with four steam generators and two turbines. The four turbines are located within a single building which is situated between the reactor buildings.

A short study was made of a much larger plant (740 MWE) and a significant reduction in power cost resulted. Use of helium as coolant was investigated and found to offer no advantage over CO_2.

The approach to this design was with a more optimistic attitude than would normally be assumed in preliminary analysis, because the purpose here is evaluation of a design which has been completed and for which a power production cost of 7.7 mils/kwh is quoted. Hence, for evaluation purposes it was assumed that all important technical problems are either solved or solvable and that the usual heavy allowances for factors of safety, error, and contingency would not be necessary. Thus, the design policy was to allow the benefit of doubt to the existing plant. A conservative attitude was adopted, however, in the cost and economic studies, and the results are believed to be realistic and safe, though not excessive.

The construction cost in the United States of the reference design was estimated to be $67 million and power cost to be 17 mils/kwh. Parameter studies indicate that these costs could be reduced appreciably by increased reactor size.

II. REFERENCE DESIGN

by

W. F. Banks

A. INTRODUCTION

This section describes the reference design for the study, and the technical studies and calculations are discussed under Sect. III, Design Study. In addition to fulfilling one of the specific requirements of the contract, this reference design served two purposes. First, the reference design provided a basis for estimating costs. The available information on Calder Hall was meager, contradictory, and insufficient; hence, 25 per cent of the effort was scheduled for preparation of engineering drawings for this preliminary design. Second, the preparation of engineering drawings revealed problems which otherwise might have been overlooked.

B. REACTOR

A section through the reactor and vessel is shown in Dwg. 2. The core is a graphite cylinder 26 ft high and 41 ft in diameter, contained in a spherical steel pressure vessel 57 ft in diameter and 4 in. thick. Dwg. 3 shows a layout of the core. In the core are 1,824 vertical fuel channels, 4 in. in diameter and spaced on a 9.25-in. square lattice. At the center of each group of sixteen fuel channels is a control rod channel. Directly above each of the control rod channels is a 12-in. nozzle in the tank. From each nozzle a loading pipe extends up through the top shield to the loading room. Above the top shield is a valve in each pipe and above the valve a bolted flange, to which is bolted the flange of the control rod thimble.

The fuel elements (shown in Dwg. 4) are uranium bars, 1.5 in. in diameter and 40 in. long, clad in Magnox, a magnesium alloy. Six elements are stacked in a fuel channel, giving an active length of 20 ft. Each element has a spider on one end to center it in the channel, and two conical tips, male one end and female the other, by which each element is aligned with the adjacent ones. The cladding has transverse spiral fins on 0.125-in. pitch. There is no thermal bonding material between the uranium and the cladding. The uranium is grooved to prevent the cladding from slipping, since the uranium grows faster with temperature than does magnesium. Thus the cladding is stretched and stressed by the uranium. The jacket is filled with helium.

Drawing 5 shows the loading face of the top shield and the floor plan of the fuel charging room. At the side of the building is a water tank for the storage of the irradiated elements, to which access is gained through plugs in the floor.

The fuel handling procedure is as follows. The control rod whose thimble is fastened to the loading pipe through which access is to be gained is lowered into the core and disengaged from its cable, which is then withdrawn onto its drum. The valve is then closed and the thimble with the drum inside is removed by means of the overhead crane. The mobile charging machine is then positioned over the loading pipe and bolted to the flange. The valve is opened, pressurizing the charging machine, which then lowers the fuel loading chute (Dwg. 6) into the tank and positions it over one of the sixteen fuel channels to be served by it. A grab on a cable is lowered through the chute to remove the fuel element. The fuel elements are stored in a magazine which accommodates twenty-four elements, sufficient to fill four fuel channels. New fuel elements are then placed in the channels, the chute withdrawn, and the valve is closed. The machine is then removed and the control rod drive and thimble is fastened to the flange of the valve, the valve opened, and the cable lowered to grasp the control rod and withdraw it from the core. Thus reactor operation may continue during fuel changing, the power level being determined by the flux distribution with the one rod completely inside the core.

The control rod drive and details are shown in Dwg. 7 and 8. The drive mechanisms are located in thimbles above the operating floor, providing for easy maintenance. In the event of a scram, an electromagnetic clutch disengages the motor from the planetary gear, allowing the rod to fall at nearly 1 g. After an initial fall of five feet into the core, the eddy current brake starts to slow the rod descent.

C. HEAT REMOVAL SYSTEM

The heat removal system is shown in Dwg. 9. The coolant gas, CO_2, is conducted to and from the core in four pairs of 54-in. pipes, which are the same size used in the Calder Hall plant. There is a valve in each line to and from the core.

The pumps are centrifugal blowers similar to those used by the British. Because of the higher operating pressure of this reference design (350 psia vs 100 psia), the pumping requirements are much less and the motors are of 900-hp capacity compared to 1,500-hp capacity in the Calder Hall plant. Ward-Leonard drives are used in Calder Hall because of their excellent speed control and have been assumed in this evaluation, but it is possible that adequate speed control could be achieved by a less expensive method. Determination of the speed control requirements under various conditions would require considerable analytical investigation, which properly should come at the beginning of the detailed design.

Steam is generated at two pressures in four generators. The construction of the steam generators is shown in Dwg. 11. The thermodynamic cycle and heat transfer studies are discussed in Sect. V and calculations are given in Appendix B. The feedwater heating equipment is quite simple. There are two large deaerating heaters per reactor, with no additional type of feedwater heaters. Larger than usual storage tank sections of the deaerating heater are provided to ensure an ample source of preheated feedwater for emergency conditions.

D. ELECTRICAL GENERATING SYSTEM

The electrical generating system is shown in Dwg. 12. Four turbogenerators were assumed for the reference design, as at the Calder Hall plant. Manufacturers have stated, however, that the full capacity of the plant could be supplied in a single machine, which would result in lesser capital cost at the expense of flexibility.

E. CONTROL AND OPERATION

A block diagram of the nuclear instrumentation system is shown in Dwg. 15, and a block diagram of the Plant Control System is shown in Dwg. 16. Because the Calder Hall reactors are designed for plutonium production and because of the paucity of available information regarding their control systems, the system shown here for the reference design was developed from a safety study and other basic considerations and is not necessarily similar to the Calder Hall installations. A detailed description of the functioning of the system is given in Appendix D.

F. DESIGN DATA

1. Plant

Number of reactors	2
Number of heat exchangers	8
Number of turbines	4

2. Reactor

Lattice spacing	9.25 in.
Number of fuel channels	1,824
Active height, 6 elements x 40 in.	20 ft
Height of graphite	26 ft
Active diameter	37 ft
Graphite diameter	41 ft
Diameter of center fuel channel	4 in.
Number of control rods	88

3. Fuel Elements

Material	Magnesium Alloy (Magnox)
Length of uranium	40 in.
Diameter of uranium	1.15 in.
Cladding thickness	0.072 in.
Diameter at base of fin	1.31 in.
Diameter of outside of fin	2.125 in.
Fin spacing	0.125 in.

4. Pressure Vessel

Material	A212 Firebox Plate
Thickness	4 in.
Diameter	57 ft
Weight	1,720,000 lb
Design temperature	700° F
Design pressure	335 psig

5. Operating Data

Temperature, outlet	655° F
Temperature, inlet	300° F
Coolant Pressure	350 psia

Coolant flow, total	3,500 lb/sec
Coolant mass flow rate, center channel	117 lb/ft^2-sec
Pressure drops	
Core	6.9 psi
Piping	1.0 psi
Heat exchanger	0.5 psi
Power, each reactor	
Reactor thermal	327 MWT
Gross electric	70.5 MWE
Electric pumping	5.7 MWE
Net electric	64.8 MWE

6. Steam Plant

High pressure cycle	
Turbine inlet temperature	600° F
Turbine inlet pressure	340 psia
Flow	577,000 lb/hr
Low pressure cycle	
Turbine inlet temperature	330° F
Turbine inlet pressure	60 psia
Flow	488,000 lb/hr

III. DESIGN STUDY

by

W. F. Banks

A. CALCULATION OF REACTOR POWER

1. Fuel Element

An initial search of the literature revealed that the Calder Hall fuel elements were clad in a magnesium jacket having fins whose surfaces were transverse to the direction of gas flow, which was also the direction of the axis of the element, rather than transverse to the flow, as would usually be expected. The best work on the investigation of flow resistance and heat transfer for this type of fin arrangement appeared to be that of Knudson and Katz, (Ref. 1 and 2), and their empirical formulae for the friction factor (Ref 1, p. 139) and heat transfer coefficient (Ref. 1, p. 220) were used to calculate pressure drops and power output for a number of fuel elements and flow channels whose dimensions were near the approximate ones which were quoted in the literature. These calculations established the reasonableness of the pressure drop and power output reported.

At about the time of completion of these first calculations, Reference 3 became available. Though this reference was not regarded as a completely reliable source, its description of the Calder Hall fuel element was more complete and numerically specific than any found in the classified literature, hence the dimensions given were adopted for the reference design. The correctness of these dimensions was later verified. Because it was expected that the heat transfer characteristics of the fuel element had been thoroughly studied both experimentally and analytically by the British, the Calder Hall element was adopted as a basis for this study.

The fuel material choice of unalloyed heat-treated cast slugs appeared sound. In the United States, cast slugs have been studied only briefly and the available fabrication and radiation data are limited. This is a cheap fuel fabrication method and should lead to low over-all fuel cost. The British recognize the need of a fine-grained, randomly oriented structure within the fuel material, which can be obtained by casting and heat treating of unalloyed uranium or casting of dilute alloys of uranium (Ref. 25, 26). Some American data presently being developed on alloyed cast slugs indicates excellent radiation stability with high burnup (Ref. 34, 35, 36, 37). The length of 40 in. may permit excessive bowing, but the design is such that shorter elements could be used should this prove to be the case.

2. Coolant

The advantages and disadvantages of various gases as reactor coolants will not be discussed here. The choice of CO_2 for this reactor appears sound. As is discussed later, net power output of the plant is approximately the same with either CO_2 or helium. The two important unknowns concerning its use in this reactor are: (1) stability in the presence of radiation, and (2) extent of the reaction between CO_2 and graphite. The effect of the latter is negligible below 750 ° F in the absence of radiation, the equilibrium partial pressure of CO_2 being less than 0.25 psi for a total pressure of 350 psi (Ref. 9). Information regarding these processes in the presence of radiation was not available at the beginning of this study. The British have conducted experiments with a graphite loop in BEPO, and report favorable results; hence the use of CO_2 was assumed for the purpose of this study (Ref. 8).

3. Core

A nuclear investigation (Ref. 5) indicated that the rod diameter of 1.15 in. was near the optimum for excess k, although the Calder Hall lattice spacing (7.5 in.) appeared to be optimized for plutonium production rather than multiplication factor. Calculations indicated that a spacing of 9.25 in. on a square lattice would be a better optimization for multiplication factor. A core was then laid out using this element on the 9.25-in. square spacing, and the number of channels found to be convenient was 1,824 (Dwg. 3), which resulted in a core radius of 37 ft. Gray control rods were assumed as in the Calder Hall reactor, and one-group, three-region calculations were made of the thermal neutron flux distribution across the core. This distribution was used in the power calculations, and is shown in Fig. 1; also shown is the zero-order Bessel Function distribution which is usually assumed, but which would only obtain if all control rods were withdrawn from the core. The calculated flux, being flatter, results in greater power extracted from the core. It was calculated that if a completely flat flux could be obtained the power output would be increased by 20 per cent.

An active core height of 20 ft (being that of Calder Hall) was assumed and the power output of the reactor calculated using the method described below. In making these calculations it was convenient to use the dimensionless Reactor Heat Transfer Number, designated by the Greek letter lambda, which is defined in Ref. 6, and which may conveniently be thought of as being proportional to velocity. Most of the intermediate steps in the calculations are plotted in terms of lambda.

4. Reactor Thermal Power

The neutron flux distribution whose calculation is described above was assumed for the radial direction and a cosine distribution was assumed axially. A limiting temperature of 1200° F was imposed for the fuel and the outlet temperature for the coolant leaving a fuel channel was calculated as a function of the radial position of the channel in the core. This was done for inlet temperatures of 300° F and 400° F, the results being plotted in Fig. 2 and 3, for various values of the dimensionless parameter lambda, which is proportional to coolant velocity. It is seen that the coolant temperature leaving the outer channels near the outer edge of the core approaches closely the fuel temperature of 1200° F. Because of cladding limitations, a temperature limit of 750° F in the coolant was then imposed and the mean outlet temperature calculated, the results being shown in Fig. 4.

These calculations assume that the coolant flow is orificed as required to produce the outlet temperature calculated. Thus the pressure drop across the core is determined by that of the central channel, and at all other channels the total drop, being the sum of the drops through the orifice and through the channel, must be equal to this central channel drop. Instead of an orifice, however, it is possible to reduce the diameter of the channels away from the center and take all or most of the pressure drop in the channel instead of across the orifice. This is done in the Calder Hall reactors by division of the core into three zones, each with a different diameter of channel.

This reduction in channel diameter with distance from the center has two beneficial effects. First, the transfer coefficient is increased, reducing the temperature drop across the film and consequently allowing the coolant to reach a temperature nearer that of the cladding. This results in a higher mean outlet temperature. Second, the lattice spacing may be reduced in the outer regions of the core, allowing more fuel elements and resulting in increased power. This was not done for this reactor because there was not sufficient time available to make the complicated reactivity and flow calculations required.

A temperature drop across the cladding and gas film of 200 ° F was calculated for the center of the reactor and 100 ° F in the outermost channel, as plotted in Fig. 11 and 12. Thus the hottest point of the cladding next to the uranium surface would be at 810 ° F. No information of the temperature behavior of the alloy (magnox) employed in the Calder Hall element was available, but it appeared unlikely that any form of magnesium could exceed that temperature and retain its shape. The best alloy of magnesium for high temperature service available in the United States appears to be Dow HM21XA-T8 (Ref. 27), which contains 1.5-2.5 per cent thorium and 0.35-0.8 per cent manganese. The best performance claimed by Dow for this alloy is 100 hours exposure at 700 ° F, which is far short of the 810 ° F which the cladding would be required to withstand for several years. However, the structural duty for which this performance is claimed is not stated, and for the application considered here there is no strength requirement because the cladding is stretched by the growth of the uranium bar; the only structural requirement on the cladding being that the fin support its own weight and resist the bending load applied by the gas stream. Hence it is reasonable to expect that the material would retain its shape at temperatures greater than the 700 ° F, but on the basis of the information referred to it is not possible to estimate how much. However, in view of the lack of specific information it was decided to proceed with the calculations, limiting the coolant temperature to 750 ° F. This subject would require careful consideration and thorough experimental and analytical investigation before the detailed design of such a reactor as this could proceed. In fact, the fuel cladding performance is the only major uncertainty of the entire plant.

The one-group neutron flux distribution referred to previously was integrated numerically and an average-to-peak ratio of 0.575 was obtained. The power of the central rod in the core was calculated using equation (7) in App. A, which, when combined with this average-to-peak ratio and the number of fuel channels determined by the core layout gives the thermal power of the reactor.

The mean outlet temperature of the coolant leaving the reactor was evaluated by a power-weighted average of the outlet temperature of the individual channels, which is shown in Fig. 4 for the limited case. Using this temperature, a heat balance was calculated to determine the over-all plant efficiency which, when applied to the thermal reactor power previously calculated, gave the electrical power of the steam plant, after deduction for steam plant auxiliaries, but before deduction for coolant pumping power.

As explained above, the pressure drop through the central channel sets the total drop everywhere across the core and orifice plate. Hence to determine the coolant pumping power, the density of the coolant was evaluated (Ref. 7) at the arithmetic average of the inlet temperature and the central channel outlet temperature from Fig. 2 and 3, corresponding to a particular value of lambda. The power of the central channel was calculated using equation (7), App. A, and from its power and temperature rise the mass flow and pressure drop followed. The pumping power was then calculated using this pressure drop, plus a 1.5 psi drop for the ducts and heat exchangers, and using the density of the coolant evaluated at the cold leg temperature, i. e., the temperature at the pump. An over-all pumping efficiency of 50 per cent was assumed from the busbar to the moving gas and the busbar pumping power requirement was calculated, which was then subtracted from the previously calculated plant electrical output to find the net electrical output of the plant.

The results of these calculations are plotted in Fig. 5 for a 300° F inlet temperature and operating pressures of 200, 250, 300, 350 psia. and in Fig. 6 for an inlet temperature of 400° F and the same range of pressures. It is seen that for a given operating pressure there is a value of lambda (and of velocity) which gives the maximum power output for the reactor, and, as shown in Fig. 9, the maximum power output of the reactor increases with increased operating pressure. Reference to Fig. 4 then gives the mean outlet temperature at which this maximum power is delivered.

In adopting the above approach, it has been assumed that the capital investment of the plant will be so large that maximizing total power is of primary importance and operating efficiency will be of secondary importance. Or, stated differently, that the major component of power cost will be capital charge, which was found to be the case. This is discussed in Sect. IV-H, paragraph 1.

5. Pressure Vessel

A study was made of large pressure vessels and it was concluded that 4 in. would be near the maximum practical thickness for field fabrication. One vendor, Chicago Bridge and Iron, states that it is constructing facilities with which plate for the spherical tank could be stamped in thicknesses up to five inches. This vendor also states that field fabrication of this thickness is feasible, but that there is no X-ray machine available for field inspection of this thickness.

Both cylindrical and spherical vessels were laid out around the core (Dwg. 3) and operating pressures allowable under the ASME Pressure Vessel Code were computed for each. The allowable pressure for the spherical tank was found to be about 100 psia more than for the cylindrical to house the same core (Dwg. 3). A check of the power vs pressure plot of Fig. 9 indicated that this pressure difference would mean a greater output of 5 megawatts for the spherical tank. This greater output would be obtained at the expense of a higher tank cost for the spherical tank and additional building and shield cost. Although a cost estimate had not been prepared for the plant, a rough estimate indicated that these additional costs due to the spherical tank would be $700,000. If the entire reactor portion of the plant should cost $350,000 per MWE, the addition to the capital value of the plant by the adoption of the spherical tank would be five times this amount, or $1,750,000. The spherical tank thus appeared an economical choice and was adopted for the reference design. In the design of a plant of this type this point would merit a thorough investigation after accurate cost estimates were available.

A design study of control rod and fuel changing mechanisms had been made (Dwg. 6, 7, 8) to ensure that a feasible approach was being made and to determine the headroom requirement between the top of the core and the top of the tank. The diameter of the sphere which would accommodate the core and satisfy the headroom requirement (Dwg. 3) was found to be 57 ft. For A212 Firebox Plate and a design temperature of 700° F the ASME code specifies a maximum pressure of 335 psig at that diameter, and this pressure was adopted for the reference design.

6. Determination of Coolant Temperatures

With the operating pressure fixed at 350 psia, reference to Fig. 5 and 6 indicated a net plant output of 64.8 MWE for an inlet temperature of 300° F and 63.2 MWE for an inlet temperature of 400° F. Reference to Fig. 4 indicates a mean outlet temperature of approximately 655° F for the first case. Calculations of the heat exchanger surface requirements and temperatures were carried out in complete detail, for both 300° F and 400° F inlet temperatures to the reactor, and an additional requirement of 375,000 sq ft of exchanger surface was found for the lower temperature. On the basis of a preliminary price of $1.00/ft of surface which had been suggested by a vendor, this indicated a cost of $375,000 for surface. However, the coolant pumping power at the lower temperature is less, 5.8 MWE being required to deliver 64.8 MWE net of power for that temperature, or 8.2 per cent pumping power; while for the 400° F inlet temperature 7.1 MWE are required to deliver 63.2 MWE net busbar power, or 10.1 per cent pumping power (Fig. 18, 19). Thus the pump investment is 1.3 MWE less for the 300° F operation, which would about offset the additional exchanger investment. Thus the 300° F appeared to offer 1.6 MWE of electrical capacity for little or no additional investment, and was adopted as reactor inlet temperature for the reference design. An inlet temperature lower than 300° F to the reactor was not considered because the heat exchanger calculations had shown that the surface requirements would increase rapidly for a lower gas temperature.

7. Use of Helium as Coolant

Using the temperatures, pressures, and flow rates thus established, preliminary design drawings were prepared and a cost and economic study was begun. Meanwhile, the calculations described above were repeated for the case of helium coolant in lieu of CO_2. The results are plotted in Fig. 7 (for an inlet temperature of 300° F), indicating a net power output of 65.5 MWE as against 64.8 MWE for the CO_2 cooled case. This unexpected result shows that there is no incentive to assume the additional expense and handling difficulties which would result from the use of helium.

Because it had been expected that helium would prove significantly better than CO_2, a detailed comparison of the calculations was made to pinpoint the reason for the small difference in reactor output. In comparing these results, the following relationships are found to be significant.

$$P_{net} = P_{Electric} - P_{Pump}$$

$$P_{Electric} = \text{Efficiency} \times \text{UA} \times \text{Constant}$$

where the constant is given by equation (7), App. A, and the number of fuel elements in the reactor.

$$UA = \frac{L}{R_{film} + R}$$

wherein the notation is

UA = Over-all heat transfer coefficient from center of uranium to coolant.

L = Length of fuel element

R = Thermal Resistance, other than film

The relatively high conductivity of helium results in a substantially lower film resistance R_{film} (approximately 25 per cent) (Fig. 16). However, the total resistances of the fuel element, R, is approximately three times R_{film}, and efficiency is approximately 25 per cent; thus a 25 per cent advantage in film resistance results in an effective power advantage of only 2 per cent. This small advantage is further reduced by an additional pumping power requirement, so that the net effect is a net power increase of only 1 per cent for the helium.

8. Effect of Increased Reactor Size

From the studies described above, there appeared to be three possibilities of increasing the power rating of the reactor: (a) Increase the operating pressure, which would increase reactor output, as shown in Fig. 9. The relationship given in this figure, however, assumes a constant number of fuel elements. Because tank thickness was limited to 4 in. by fabrication feasibility, increasing operating pressure would reduce the tank size, resulting in fewer fuel elements, which in turn would enhance the attractiveness of the second possibility: (b) Increase the tank diameter (holding constant the tank thickness, because this was dictated by feasibility of field fabrication), thereby decreasing operating pressure but increasing the number and length of the fuel elements. However, increasing the length of the elements (and core height) would have required more calculation time than was available, hence the variation studied was in core diameter only.

A third possibility, (c) was the decrease of lattice spacing to increase the number of fuel elements. This was not thought to be desirable in the reference design, because the additional excess k obtainable from the greater lattice spacing would allow flux flattening to increase power, and at the same time provide desirable flexibility in that the excess k could be used to obtain additional burnup, should the fuel element prove able to allow it. A detailed optimization study of lattice spacing vs flux flattening was beyond the scope of this study, but should be carried out before beginning the detailed design. For core sizes larger than the reference design, however, it was expected that increase in excess k with diameter would be insignificant, hence for larger cores the reduction of lattice spacing becomes attractive.

Thus the most attractive possibility for increasing reactor rating appeared to be a combination of (b) and (c). The maximum vessel diameter for a constant thickness of 4 inches was computed for operating pressures of 300,250, and 200 psia. Power output was calculated for the largest diameter core which could be accommodated in each vessel, assuming the same 20-ft height of active element. The results are given in Fig. 8, from which it is seen that power output increases approximately as the 2.6 power of the diameter. For the 200 psia case, the allowable tank diameter, with a 4 in. thickness, is 95 ft. A tank of this size would accommodate a core of 85-ft diameter. A reduced lattice spacing of 7.5 in. was assumed for this 200 psia case for the reasons mentioned above. Because this lattice spacing would not permit much flattening of the neutron flux, a zero order Bessel Function was assumed to represent the radial flux distribution and the power output was calculated as before. The results are plotted in Fig. 17. Capital and power costs were then estimated on the same basis used for the reference design, as discussed in Sect. IV, paragraph I.

9. Use of Enriched Fuel

If a completely flat radial neutron flux distribution could be obtained, the power output at the same temperature would be increased 20 per cent for the reference design. By the use of slightly enriched fuel in the outer regions of the core, a considerable degree of flux flattening would be effected, increasing power production at the expense of an increase in fuel cost. Because fixed costs far outweigh fuel costs in a high-investment plant of this type, there is a strong incentive to use enriched uranium. There are no apparent advantages in the exclusive use of natural uranium in the United States.

B. FURTHER STUDY - RESEARCH AND DEVELOPMENT

There are a number of studies and tests that would be required before initiating detailed design work of this type of plant. Some of the more important are mentioned here.

A thorough investigation of the heat transfer characteristics of the fuel element should be made for various fin dimensions and spacings, and conclusions should be verified by test. Experiments should also be conducted upon warped and bowed elements to estimate the decrease in heat transfer coefficient resulting from exposure in the reactor, because a significant decrease might result in overrating the plant. The efficiency of the transverse fin arrangement depends upon the relationship between fin height and spacing; hence the transfer coefficient may be expected to change and probably decrease as the geometry is affected by exposure. Pressure drops should also be investigated.

Models should be constructed of the tank and core and experiments conducted to determine coolant flow patterns and distribution to the fuel channels.

An extensive investigation of cladding possibilities was made by the British, the results of which should be reviewed (Ref. 28). The compatibility of beryllium with CO_2, CO, and uranium should be investigated, because it withstands higher temperatures than magnesium and also is a good moderator.

C. EXPECTED TECHNOLOGICAL IMPROVEMENTS

In preparing the reference design described above the objective was to develop the preliminary design of a plant which could be designed, built and operated within five years, and it is believed that the design presented fulfills this aim. Except for the problems with the fuel element, discussed elsewhere, the plant is conventional. As a result of this, little is to be expected in the way of technological improvement.

The power production of the reactor is subject to two temperature limitations. One of these is the limiting temperature in the uranium, assumed to be 1200° F in this case, which applies from the center of the reactor out to the radius at which the temperature limitation of the cladding becomes effective. This is discussed in Part A above and is shown in Fig. 2 and 3. Expected improvements in uranium metallurgy would allow this temperature limit in the uranium to be raised, increasing the power output of the plant. However, some alloys now being studied for enriched reactors may not be applicable to a natural uranium reactor of this type because of the poison used in alloying.

The second temperature limitation is that of the fuel cladding, also discussed in Part A. An improvement in cladding performance which would allow a higher temperature would not increase the power extracted from the core, but would increase the mean outlet temperature at which it is delivered. Fig. 10 is a plot of mean outlet temperature without the 750° F limit in the coolant, for an inlet temperature of 300° F. For a value of lambda of 3.25, at which the maximum power is delivered in the case of 350 psia operating pressure, the mean outlet temperature is 705° F as against 660° F in the limited case (Fig. 4), a difference of 45° F. The over-all plant efficiency would benefit from this increase. Figure 11 is a plot of the temperatures of the cladding and coolant along the central channel of the core, and Fig. 12 is a similar plot for an outer channel.

IV. ECONOMIC STUDY

by

W. T. Morgan

A. CONDITIONS AND ASSUMPTIONS

1. The AEC contract for this study specified certain conditions. The conditions affecting the economic study are:

    a. Operation is to be by a private utility in the United States; no government financial assistance is to be assumed.

    b. Prices for SNM are to be as quoted by AEC in "Confidential" list. (Later values published in a news release dated November 18, 1956, are quoted herein).

    c. Any plutonium produced is to be credited at "fuel" value only.

    d. The average plant factor over the life of the plant is 60 per cent.

    e. The plant life is 40 years.

2. The following economic assumptions were used:

    a. Construction is at current U. S. prices. No escalation is included.

    b. Utility financing is assumed to be 50 per cent bonds, 20 per cent preferred stock, and 30 per cent common stock. Refer to Section IV-D, 1.

    c. Total fixed charges are assessed at 15 per cent. Refer to Section IV-D, 3 and 4.

    d. Fuel lease charges are 4 per cent per year on the initial dollar value of the fuel.

    e. Start-up costs are included in the capital cost.

    f. Cost of manufacturing the first core loading is not included in the capital cost. Refer to Section IV-C, 2.

    g. Interest on money tied up during the engineering and construction period is included in the capital cost.

    h. Payroll overheads are assumed to be 20 per cent of direct wages.

i. Indirect operating costs are assumed to be 50 per cent of the direct operating costs.

j. Maintenance is arbitrarily estimated at 0.6 mils/kwh.

k. Operating supplies are arbitrarily estimated at 0.1 mils/kwh.

l. The average fuel exposure level is assumed to be 3,000 megawatt-days per tonne (tonne = 1,000 kilograms).

B. PLANT COST

1. The construction cost of the plant is estimated to be $67 million:

2 Reactors, plus auxiliaries.	$34.0	Million
4 Turbogenerators, plus auxiliaries . . .	15.5	Million
Land, buildings, etc.	4.5	Million
Engineering and miscellaneous	13.0	Million
Total	$67.0	Million

For a net output of 130,000 electrical kilowatts, this cost amounts to $515/kw. Other estimates of Calder Hall-type plants are shown below.

Type	Electrical Output	Location	Cost/kw	Reference
Calder Hall "O"	92 MW gross, approx. 73 MW net.	U.K.	$610	(17)
Calder Hall Stage 1	Probably 150-200 MW	U.K.	$340	(18)
Calder Hall estimate	90 MW net	U.S.A.	$638	(18)

2. The cost of the reference design described here was obtained by estimating the cost of the present British Stage "O" plant as if built in the USA, then factoring in the differences proposed for the reference design (refer to Appendix C).

After an independent check of British vs American costs, the same conclusion was reached as in Ref. (18). This reference states that a plant built in the USA would cost about 50 per cent more than the same one built in Britain. This check was made by comparing costs from Engineering News Record (Ref. 21) and by personal communications with local architect-engineer organizations that are doing business in both countries. The present British Calder Hall has been widely quoted as costing 16.5 million pounds sterling, or $46 million based on an exchange rate of \$2.80. Applying the 50 per cent scale-up for American construction, this same plant would cost approximately \$69 million in the U.S.

The reference design shown here is expected to cost slightly less than a Calder Hall Stage "O", despite the greater power output. This lower cost is caused principally by the lower estimated costs of steam generators, CO_2 circulators, and engineering. The reactor itself and the turbine-generator equipment are expected to cost more than Stage "O".

C. OTHER CAPITAL COSTS

1. Interest on construction money is assumed to be 6 per cent. It is further assumed that 1/2 the construction cost is tied up over a construction period of 4 years.

2. The cost of manufacturing the first core loading is not included as a capital cost in this analysis. It is true that money must be raised in the market to provide the first loading of fuel. Likewise, money must be set aside during the life of the plant to pay for burn-up and reprocessing costs of the core loading remaining when the plant is finally shut down. Offsetting the above expenses is the fact that no fabrication, reprocessing, or burn-up charges need be paid for about three years after starting reactor operation, because the first fuel elements will not be removed until that time. Subsequently, a regular fuel change of about 310 fuel elements per month for the life of the plant is initiated. One might start making replacement elements in quantity perhaps six months before the first element is removed. Reprocessing and burn-up costs will not be incurred for about six months after the first element removal. During these first three years, however, the plant operator will have received about \$2.2 million per year (3.17 mils/kw) to cover this segment of power cost. Converting these fuel receipts and costs (fabricating the first core, reprocessing after plant shutdown, and burnup after plant shutdown) to present value, the costs nearly cancel the receipts.

Because these factors are compensating, they are omitted in subsequent calculations.

3. Start-up costs are estimated at \$800,000. This includes start-up personnel for approximately 1 year, plant data books, and expendables.

4. Miscellaneous construction, shipping, spare parts, and other costs are estimated at \$1 million.

D. FIXED CHARGES

1. The fixed charge to be applied to capital is principally dependent on three items: return on investment, tax rates, and depreciation. Ref. (19) gives the following breakdown as to the capital structure and return for a typical utility:

	Per cent of Total Investment	Rate of Return	Weighted Return	
Bonds	50%	3.5%	1.75%	
Preferred Stock	20%	5.5%	1.10%	3.95%
Common Stock	30%	9.5%	2.85%	
	100%		5.7 %	

These figures also agree generally with those given by Davis at Geneva, (Ref. 24) for a private utility.

The tax rates used in this study are: ad valorem, 2 per cent; State taxes, 4 per cent; Federal taxes, 52 per cent.

Miscellaneous costs are assumed to be 0.2 per cent. This includes insurance, which is presently a very debatable subject.

2. Depreciation is considered to be based upon the straight line method over a period of 40 years. This method was selected because it appears to be the practice of the majority of utilities in the U. S., as reported in the 1954 Federal Power Commission Report FPC S-113. (Ref. 22)

No additional allowance has been made for excess replacement of components that do not last 40 years. A certain amount of this replacement may be included in the maintenance charge, while other items should definitely be charged to capital replacement. Because of the difficulty of estimating the life of reactor components, and because a 40-year life was stipulated in the conditions of the study, such extra replacement was neglected.

3. Based upon the foregoing, the total fixed charge rate is 15 per cent: (refer to next page)

Gross return on investment			15.0%
Deductible before State and Federal taxes:			
	Bond interest . . .	1.75%	
	Ad valorem taxes . .	2.00%	
	Depreciation . . .	2.50%	
	Miscellaneous . . .	.20%	
		6.45%	6.45%
Balance subject to State taxes			8.55%
State taxes @ 4 per cent			.34%
Balance subject to Federal taxes			8.21%
Federal taxes @ 52 per cent			4.27%
Net for preferred and common stock (3.95% assumed to be required)			3.94%

The 15 per cent gross return on investment is in agreement with the figure used by other evaluation groups, such as Project Dynamo (Ref. 29). It is 1 per cent higher than the average value of fixed charges for oil-fired plants, as stated in the 9th Steam Station Survey, Electrical World, October 1955. (Ref. 20). It appears logical that the rate of return might be higher on a nuclear plant than a conventional plant because of the uncertainties involved. On non-property items, such as carrying charges during construction and start-up costs, ad valorem taxes are omitted; this brings the fixed charge rate down to 13 per cent for these two items.

4. The use of this 15 per cent fixed charge rate requires some discretion regarding the plant factor with which it is coupled. If the 15 per cent is applied against the initial investment, the cost calculation should be made using a high plant factor of 80 to 85 per cent. This gives the power cost on a "first year" basis. (Ref. 30, 31). Conversely, if one uses the average plant factor over its entire life, the 15 per cent fixed charge rate should be applied against the depreciated book value of the plant, year-by-year. This gives the power cost on a "lifetime" basis. The calculation can be simplified by using average interest (since straight line depreciation was used) and arriving at a new apparent fixed charge rate which is applied against initial investment. (refer to next page)

Gross return.		15.00%
Less 1/2 of equity return . .	1/2 x 5.7%. .	2.85
Less 1/2 State income taxes .	1/2 x .34%. .	.17
Less 1/2 Federal income taxes	1/2 x 4.27% .	2.13
Apparent return on initial investment.		9.85%

5. Because there does not appear to be general agreement on the calculation method to be used, two are presented in paragraph G. The difference in cost by the two methods described in the preceding paragraph ("first year" method, and "lifetime" method) is about 0.5 mil/kwh.

E. FUEL COSTS

1. The fuel element used in this reactor is described in Section III-A. The cost of producing one 40-in. element is estimated to be slightly under $200, of which $1.50 per pound of U is the charge for producing the slug, and $154 per element as the canning cost. The cycle starts by obtaining natural U as billets at a cost of $40/kg. The slugs are formed by vacuum casting and heat treating, with only minor machining required to crop the ends and clean up the casting. Some alloying may be necessary to achieve 3,000 MWD/tonne burnup, but the alloying cost is considered negligible. The finned casings are produced by milling fins in "Magnox" tubing.

2. After irradiation, the fuel elements are allowed to "cool" for 3 months before shipment to the chemical processing plant. Shipping the irradiated elements, which is estimated here at $5.00 per pound of uranium, will be an appreciable part of the total reprocessing cost. At the process plant, the elements will be dejacketed, processed to nitrates, the uranium processed to UF_6, and the plutonium processed to metal buttons.

3. The "hold-up" in the fuel cycle is expected to be 1 year, exclusive of time in the core. The fuel cycle diagram is shown in Appendix C.

F. OPERATING COSTS

1. Total yearly direct wages are estimated to be about $360,000. To this is added 20 per cent for payroll overheads, and 50 per cent for indirect operating costs.

2. Maintenance is arbitrarily taken as 0.6 mils/kwh, which is approximately twice the figure quoted in Ref. (20) for conventional stations.

G. COST OF POWER - REFERENCE DESIGN

1. "First Year" basis, using 15 per cent fixed charge rate applied against initial investment. Plant factor is 85 per cent.

Item	Capital Cost (Millions)	Fixed Charge Rate	Yearly Cost in Millions	Mils/kwh @0.85 P.F.
Fixed Costs				
Construction	$67.0	15%	$10.05	10.40
Capital Charge during Construction	8.0	13*	1.04	1.08
Start-up Costs	0.8	13*	.10	.10
Miscellaneous	1.0	15	.15	.15
Subtotal	$76.8		$11.34	11.73
Fuel Costs				
Fabrication, Reprocessing and Shipment			$ 2.55	2.65
Burn-up less Pu and Spent U Credits			.56	.58
Fuel Lease Charge			.56	.58
Subtotal			$ 3.67	3.81
Operation				
Wages Plus Payroll Overheads			$ .432	.45
Indirects			.216	.22
Maintenance			(.580)	.60
Supplies			(.096)	.10
Subtotal			$ 1.324	1.37
Total	$76.8 Million		$16.33 Million Per Year	16.91 Mils/kwh

*Does not include ad valorem taxes.

2. "Lifetime" basis, using 9.85 per cent apparent fixed charge rate applied against initial investment. Average plant factor is 60 per cent.

Item	Capital Cost (Millions)	Apparent Fixed Charge Rate	Yearly Cost in Millions	Mils/kwh @0.60 P.F.
Fixed Costs				
Construction	$67.0	9.85%	$ 6.6	9.7
Capital Charge during Construction	8.0	7.85*	.63	.92
Start-up Costs	0.8	7.85*	.063	.09
Miscellaneous	1.0	9.85	.098	.14
Subtotal	$76.8		$ 7.391	10.85
Fuel Costs				
Fabrication, Reprocessing and Shipment			$ 1.80	2.63
Burn-up less Pu and Spent U Credits			.39	.57
Fuel Lease Charge			.52	.76
Subtotal			$ 2.71	3.96
Operation				
Wages Plus Payroll Overheads			$ .432	.63
Indirects			.216	.31
Maintenance			(.410)	.60
Supplies			(.068)	.10
Subtotal			$ 1.126	1.64
Total	$76.8 Million		$11.23 Million Per Year	16.45 Mils/kwh

*Does not include ad valorem taxes.

H. EVALUATION

1. The cost of power has been estimated to be between 16 and 17 mils per net kilowatt-hour, based on assumptions described previously. This is approximately 2 1/2 times the cost of conventional power generated by private utilities in the USA. The principal reason for this difference is the very large fixed charge borne by the reactor plant, as shown in the comparison of "first year" costs below. Fuel costs are presently a stand-off, with the expectation that nuclear fuel costs may decline in the future, while fossil fuel costs are generally on the increase. The nuclear plant is again at a disadvantage in operating costs, because of the extra manpower required, and anticipated larger maintenance costs.

	Calder Hall (This Study)	Typical Conventional Thermal Plant (20)	Ratio Nuclear to Conventional
Fixed charges	11.7 mils/kwh	3.13 mils/kwh	3.8
Computed at	(15%)	(14%)	
Fuel charges	3.8	2.85	1.3
Operating charges	1.4	.54	2.6
Total	16.9 mils/kwh	6.52 mils/kwh	2.6

2. The cost of power estimated here is approximately twice the cost advertised by the British. This is caused principally by the difference in financing methods. Where a private utility in the USA would use 14-15 per cent fixed charges, the British quote 6 per cent on the conventional portion, and 9 per cent on the reactor portion. Using these values, and using the cost comparison referred to in Section IV-B, 2, it is possible to arrive at a power cost of approximately 8 mils/kwh. This is a legitimate figure for the financing system used, and compares closely with British advertised figures. However, this 8-mil figure cannot fairly be compared with 6-7 mil conventional power in the USA. Rather, the 8 mils should be compared with the low cost power generated by the steam plants of TVA, for example, which are financed at low rates. The conclusion again is that nuclear power from this reactor is about 2 1/2 times the cost of conventional power.

3. Because the gas-cooled reactor of this type is inherently one of large first cost, it is essential that this cost be spread over the greatest number of kilowatt-hours. Increasing the size would be quite beneficial in reducing unit

costs, because first costs will not go up as fast as power output. The 130,000 net kilowatt plant described in this report is not nearly large enough. Enlarging the plant also helps reduce the per-unit operating costs, since the same number of men in the operating crew can probably handle a large reactor as easily as a smaller one. (Refer to Section IV-I).

4. Anything that can be done to keep the plant factor high is extremely beneficial to the cost picture. The 60 per cent average lifetime plant factor used in this study (which is one of the conditions of this study) is probably slightly optimistic (See Ref. 31). Nevertheless, if the plant can be run at high capacity, so much the better.

5. The 40-year specified plant life does not have a large effect on the cost of power. Varying the specified life changes only the depreciation figure, which is in turn a small percentage of the total fixed charge rate.

6. Variations in cost of land, electrical transmission, condenser cooling water, etc., with different locations was not considered in this study. The cost of land will largely be overshadowed by the huge construction cost anyway. The best location would be adjacent to the fuel reprocessing plant (similar to the British Calder Hall), because this would reduce the large shipping charges on irradiated fuel. Perhaps a large reactor plant or group of plants could economically support a reprocessing plant as part of the over-all plant complex.

7. Uncertainties - The effect of uncertainties in the analysis are briefly described below.

a. Plant Cost - Probably within ± $10 million. This would affect power cost by ± 1.5 mils/kwh.

b. Fuel Costs - Fabrication and reprocessing costs might be ± 50 per cent. This would affect power cost by ± 1.5 mils.

c. Operating Costs - Maintenance costs are presently indeterminable, and might well turn out to be many times the figure quoted. Barring this, perhaps ± 1 mil might be reflected in power cost.

d. Miscellaneous - Insurance, taxes, cost of money, etc., all have a strong bearing on the fixed charge rate. No attempt is made here to assess possible changes.

e. Summary - Based on the foregoing, the uncertainty in the cost of power is probably ± 4 mils/kwh.

I. INCREASED PLANT SIZE

1. Section III-8 describes a very large reactor plant of the same general type as the reference design. The cost of this large plant is estimated at $190 million. This figure was arrived at by estimating the reactor vessel and concrete cost, and scaling up the other costs from the reference design.

2 Reactors, plus auxiliaries. . . .	$105 million
4 Turbogenerators, plus auxiliaries .	58
Land, buildings, etc.	11
Engineering and miscellaneous . . .	16
	$190 million

For a net plant output of 740,000 kilowatts, the cost per kilowatt is $256.

2. Fuel cost was not calculated, but was taken the same as the reference design case. Because the specific power of the reactor was not appreciably changed in the larger size, this assumption appears valid. Some economies in unit cost might be expected with the larger plant, because of the increased volume of fuel handled.

3. The unit cost of maintenance and operating supplies was taken the same as the reference design. Wages and indirects were reduced by the ratio of the plant outputs, because the same crew of operators can probably run either the small or the large plant.

4. Cost of Power

Item	Capital Cost (Millions)	Fixed Chg. Rate	Yearly Cost (Millions)	mils/kwh "1st Yr." (Note 1)	mils/kwh "Lifetime" (Note 2)
Fixed Costs					
Construction	$190.0	15 %	$28.5	5.2	
	190.0	9.85%	18.8		4.85
Capital Chgs.	23.0	13	3.0	.54	
During Constr.	23.0	7.85	1.8		.47
Start-up	1.0	13	.13	.02	
	1.0	7.85	.078		.02
Misc.	1.0	15	.15	.03	
	1.0	9.85	.098		.03
Subtotal	$215.0			5.79	5.37
Fuel Costs					
(Taken same as reference design)			--	3.81	3.96
Operating Costs					
Wages			.432	.08	.11
Indirects			.216	.04	.06
Maintenance			--	.60	.60
Supplies			--	.10	.10
Subtotal				.82	.87
Totals	$215			10.4	10.2

Note 1: "First Year" cost uses 15 per cent fixed charge rate and .85 plant factor.
Note 2: "Lifetime" cost uses 9.85 per cent fixed charge rate and .60 plant factor.

5. As shown in the preceding table, a very large plant might be able to provide power at about 10 mils per kilowatt-hour. While the plant described has two large reactors and a total output of 740 electrical megawatts, it appears possible to get into this power cost range with a slightly smaller plant. If half of the large plant described (i.e., one reactor instead of two) were built, power cost would not be much higher than 11 mils. The economical size range for this type of reactor would thus appear to be about 350 megawatts and up.

V. THERMODYNAMIC CYCLE

by

G. A. Schneider

As explained in Appendix D, the objective of the plant control system is to vary flow rates to meet reasonable load demand changes without change in system temperatures. Uniform system temperatures are required to minimize thermal stresses. A dual pressure steam cycle provides a means of maintaining uniform reactor temperatures. An inherent characteristic of single steam cycle systems is a decrease in rate of heat transfer which is accompanied by a decrease in temperature drive across the heat exchanger, resulting in lowering of CO_2 gas temperatures returning to the reactor. By throttling the low pressure steam flow and allowing the low pressure steam pressure to rise, the gas temperature returning to the reactor is held constant. The high pressure steam is held constant.

For the dual pressure steam cycle, there is no direct method of determining the optimum division of power between the low and high pressure cycles, and several complete heat exchanger calculations must be made and heat exchanger surface requirements determined. The final set of these calculations for the reference design is given in Appendix B.

The temperature-entropy diagram of the dual pressure cycle is shown in Fig. 13. A turbine exhaust condition of 2 in. of Hg and a moisture limit of 12 per cent were assumed and a turbine expansion line which was considered reasonable was plotted from this point. The primary consideration in selecting the low pressure steam conditions is to ensure that the temperature and pressure of the low pressure steam entering the turbine are matched to that of the steam inside the turbine passing the admission point (point H in Fig. 13). The apportionment of power between the low and high pressure cycles must then be made at these conditions so that the heat exchanger surface requirements are near a minimum as a result of reasonable heat exchanger temperature approaches. The detailed heat balance on the heat exchanger for the reference design is shown in Fig. 14 and the temperature-surface diagram in Fig. 15. The plant heat balance is shown in Dwg. 14.

For fossil-fired central steam electric plants, representative gas temperatures are :

Flame in furnace	2,400-2,500° F
Flue gas leaving furnace & entering superheater	1,900° F
Flue gas leaving superheater & entering boiler	1,600° F
Flue gas leaving boiler & entering economizer	700° F
Flue gas leaving economizer & entering air heater	500° F
Flue gas leaving air heater & entering stack	300° F

For a Calder Hall type of reactor, the maximum CO_2 gas temperature leaving the reactor is approximately 650° F. This gas temperature leaving the reactor corresponds to a flue gas temperature in fossil-fired plants at the stage of leaving the boiler, after the entire superheating and boiling has been completed. To transfer heat at 650° F with as poor a transfer medium as CO_2 requires the use of extended surface on the gas side of the exchanger tubing. The extended surface is necessary so that the outside gas film transfer coefficient approaches the inside water film coefficient.

For this study an extended fin tube was selected for the economizers and boilers (as shown in Dwg. 11) to obtain as much extended surface as possible. Bare tube exchangers were selected for the superheaters because the expected superheated steam film coefficient is as poor as that of the gas. The extended tube surface shown is fabricated by cutting a continuous groove on the outside surface of the steel exchanger tube. A preformed spiral shaped aluminum ribbon 0.05 in. thick and 1 in. high is inserted into the groove. The aluminum fins are then mechanically bonded to the tube by crimping the edges of the spiral groove against the aluminum fin. This type of extended surface has been used extensively in American heat exchanger design for more severe temperature conditions. The aluminum fin has the advantages of five times the thermal conductivity and one-third the weight of the steel fins used in Calder Hall. The British avoided the use of aluminum in the heat exchanger because of its reaction with magnesium, but if the tubing is properly cleaned before installation it is difficult to see how it could contaminate the fuel cladding in the reactor, because aluminum does not react with CO_2. The ratio of the extended fin surface to bare tube surface is 23:1 while the ratio in the Calder Hall heat exchangers is 4:1. This advantage results in a substantial reduction of the pressure vessel housing the heat exchangers and the total linear feet of heat exchanger tubing. Although a direct comparison is not possible with the British Phase "O" Calder Hall design because of the difference in thermal power, CO_2 operating pressures etc., the total linear feet of tubing per reactor in the reference design is 28,382 as compared to the 68,327 linear feet in the Calder Hall plant.

VI. CONCLUSION

by

W. F. Banks

This report presents a preliminary design of a gas-cooled, graphite-moderated, natural uranium nuclear power plant, similar to the British Calder Hall plant and employing present-day reactor technology as developed by the British. Significant characteristics of the design presented are:

1. Two reactors producing a total of 654 MWT.
2. Gas pressure 350 psia, with bulk outlet temperature of 655°F.
3. Net overall efficiency of 20 per cent, producing a total of 130 MWE.
4. Construction cost of $67 million for the entire plant.
5. Busbar power cost of 17 mils/kwh, based on a plant factor of 60 per cent over a life of 40 years, and calculated using the method of straight-line depreciation plus one-half interest (refer to footnote on p. VI-3).

A similar plant with a single reactor producing 370 MWE would have a construction cost of $100 million and power cost of 11 mils/kwh. Two reactors of this size in a single plant would result in a power cost of 10 mils/kwh.

The possibility of increasing power output by flattening the neutron flux radial distribution was considered, and it is estimated that power production of the large reactor could be increased 10 per cent which would decrease power cost by perhaps 1 mil/kwh.

Power output was also calculated with helium instead of CO_2 as coolant, and found to be only one per cent greater. There are two reasons for this unexpected result. First, although a substantial decrease in film resistance is effected by the substitution of helium for CO_2, the film resistance is only about a fourth of the total thermal resistance. Second, though helium has a high specific heat its density is low, resulting in high pumping power requirements.

Materials problems were investigated, and it was concluded that the only major uncertainty in the design is the fuel cladding performance at the required temperature. The development of another type of cladding which would permit higher gas temperatures would substantially improve the conversion efficiency of the plant.

The plant presented contains the results of considerable study of the hardware stages of design, which was necessary to ensure that a feasible approach was being taken and to enable a cost estimate to be prepared with a fair degree of accuracy. Future studies, however, should emphasize analytical work and should include investigation of the following:

1. Neutron flux flattening.
2. Decreasing fuel channel diameter away from the center of the core.
3. Decreasing lattice spacing, and varying the spacing across the core.
4. Use of enriched fuel.
5. Increased height of the core.

The basic conclusions developed from this study are:

1. A nuclear plant of this type can be constructed and operated in the United States within 5 years.
2. The cost of power from this plant would be approximately twice that from conventional plants.
3. This is inherently a high capital cost, low fuel cost plant, hence it is more attractive in situations where capital charges are low and conventional fuel costs are high. Also, the high capital cost makes large plants more attractive, since their capital charges per unit of power produced are less. Also, base load operation is desirable because of the high capital charge.
4. With present-day technology, plants of this type are not attractive for United States private utilities. However, improvements in reactor technology permitting flux flattening, higher uranium and coolant temperatures, and increased specific power could change this situation.
5. No calculations were made for the use of enriched uranium, because its consideration was not included in the contract. It is felt that this should be done, however, because slightly enriched uranium is available in the United States and because its use would result in better plant performance. Also, the recycling of plutonium with natural uranium feed might be attractive and should be investigated.

Footnote to Characteristic No. 5 (p VI-1)

The mathematical error resulting from the use of this method rather than the sinking fund method is discussed by Grant (Ref. 32, Page 87). However, in deciding which to use, the following fact should be considered.

If the utility which operates a power plant actually uses a sinking fund to amortize its investment, then and then only, a sinking fund calculation gives the correct cost of power to that utility. However, if the operating utility invests its depreciation credits annually in new plants, the actual cost of power is given by the half-interest method. That this second method more closely approaches the usual situation is indicated by two considerations. First, the return which a utility is obligated to earn for its investors is higher than that which can be obtained from a sinking fund investment. (Ref. 33, Page 139, chart of Earning Power, Growth Power, and Profit vs Plowback). It therefore behooves the utility to invest its depreciation funds in new facilities if possible; otherwise to return them to the stockholders. Second, investment in new facilities is not only most desirable from the investor's point of view, but power demand in the United States is increasing at such a rate that it is the necessary course for most utilities. (Ref. 33, Pages 138-142).

It is well to emphasize that the return to the investor is the same in either case, the difference being in the cost of power to the consumer. Thus not only good business practice but public interest urges that depreciation credits be invested in operating plants rather than sinking funds. Hence this study assumes straight-line depreciation plus half-interest.

APPENDIX A
REACTOR DESIGN CALCULATIONS

by

W. F. Banks

The average-to-peak ratio of the thermal neutron flux distribution in the radial direction is by definition

$$\sigma \overset{\Delta}{=} \frac{\int_{o}^{1} \phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\int_{o}^{1} \phi\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)} \qquad \ldots(1)$$

It is shown in Appendix D of Ref. 6 that the temperature rise of the coolant in a fuel channel is given by

$$t_2 - t_1 = 2(t_f - t_1)\left[\sqrt{1 + \tan^2\left(\frac{\pi}{2}\frac{L}{L_o}\right) - \left[\frac{2\lambda_c\,\phi\frac{r}{R}}{\sqrt{4\lambda_c^2 + \left(\frac{1}{\frac{\pi}{2}\frac{L}{L_o}}\right)^2} + \frac{\sin\left(\frac{\pi}{2}\frac{L}{L_o}\right)}{\frac{\pi}{2}\frac{L}{L_o}}}\right]^2} - \tan\frac{\pi}{2}\frac{L}{L_o}\right]\tan\left(\frac{\pi}{2}\frac{L}{L_o}\right) \qquad \ldots(2)$$

where r/R is the normalized radius at which the fuel channel is located in the cylindrical core. The outlet temperatures t_2 calculated from this equation are plotted in Fig. 2 and 3.

The mixed mean of all outlet temperatures is by definition

$$t_{2m} = t_1 + \frac{\int_{o}^{1}\left(t_2 - t_1\right) v\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\int_{o}^{1} v\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)} = t_1 + \frac{\sigma}{2\int_{o}^{1} \frac{\phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\left(t_2 - t_1\right)}} \qquad \ldots(3)$$

For convenience define

$$\Delta t' = \left[\sqrt{1 + \tan^2\left(\frac{\pi}{2}\frac{L}{L_o}\right) - \left[\frac{2\lambda_c\,\phi\left(\frac{r}{R}\right)}{\sqrt{4\lambda_c^2 + \left(\frac{1}{\frac{\pi}{2}\frac{L}{L_o}}\right)^2 + \frac{\sin\left(\frac{\pi}{2}\frac{L}{L_o}\right)}{\left(\frac{\pi}{2}\frac{L}{L_o}\right)}}}\right]^2} - \tan\left(\frac{\pi}{2}\frac{L}{L_o}\right)\right] \tan\left(\frac{\pi}{2}\frac{L}{L_o}\right) \qquad \ldots(4)$$

Then $t_2 - t_1 = 2(t_f - t_1)\,\Delta t'$

And the expression for the mean temperature becomes

$$t_{2m} = \frac{\sigma}{\displaystyle\int_o^1 \frac{\phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{(t_f - t_1)\Delta t'}} = \frac{\sigma\,(t_f - t_1)}{\displaystyle\int_o^1 \frac{\phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\Delta t'}} \qquad \ldots(5)$$

Now impose a temperature limit of 750° F on the coolant. To obtain the mean temperature t_{2m} we integrate under the $t_2 - t_1$ curve (Fig. 2 and 3) from the center line of the reactor out to the radius at which $t_2 = 750°$, and from this radius to the outside of the core we integrate under the straight line $t_2 = 750°$. The integral becomes

$$\int_o^1 \frac{\phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\Delta t'} = \int_o^{\left(\frac{r}{R}\right)_{750°}} \frac{\phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right)}{\Delta t'} + \frac{2\,(t_f - t_1)}{750 - t_1} \int_{\left(\frac{r}{R}\right)_{750°}}^1 \phi\left(\frac{r}{R}\right)\left(\frac{r}{R}\right) d\left(\frac{r}{R}\right) \qquad \ldots(6)$$

The calculation of the neutron flux $\phi(r/R)$ is described in III-A, par. 3, and it is plotted in Fig. 1. Performing the integrations indicated in Eq. (1) and (6) and substituting in Eq. (5) gives the mean outlet temperature. This is plotted in Fig. 4 for the case in which the coolant temperature is limited to 750° and in Fig. 10 for the unlimited case.

The thermal power of the central rod in the core is given by Eq. 5, Ref. 6, and is repeated here

$$q_c = UA(t_f - t_1)\,\lambda_c f(\lambda_c) \qquad \ldots(7)$$

wherein $f(\lambda_c)$ depends upon the axial power distribution and is given by:

$$f(\lambda_c) \equiv \frac{t_2 - t_1}{t_f - t_1} \qquad \ldots(8)$$

The film coefficients were calculated from the formula of Knudsen and Katz (p. 220, Ref. 1). For a cosine axial flux distribution $f(\lambda_c)$ is given by Eq. D-3, Page 28, Ref. 6.

The gross thermal reactor power is given by

$$P_G = q_c \times \text{No. Fuel Elements} \times \sigma \qquad \ldots(9)$$

The total mass flow, GA, is

$$GA = \frac{P_G}{C_p\,(t_{2m} - t_1)} \qquad \ldots(10)$$

Where C_p is evaluated at the average temperature in the core, i.e.,

$$\frac{t_{2m} + t_1}{2}\,.$$

The temperature distributions of Fig. 2 and 3 have assumed that the coolant flow in each channel except the center one is orificed in such fashion as to produce the calculated temperature distribution across the core. Thus the pressure drop across the core and orifice at any channel must equal that of the central channel. To calculate the pressure drop of the center channel we make use of its power (Eq. 7 above) and calculate its mass flow.

$$G_c = (\rho v)_c = \frac{q_c}{A_f\, C_p\, (t_2 - t_1)_{\left(\frac{r}{R}\right) = 0}} \qquad \ldots(11)$$

wherein $(t_2 - t_1)_{\left(\frac{r}{R}\right) = 0}$ is found from Fig. 2 and 3. It is the coolant temperature leaving the central channel.

The pressure drop through the center channel is then given by

$$\Delta p_{core} = \frac{f}{2g}\,\frac{G_c^2}{\rho_c}\,\frac{L}{D_e} \qquad \ldots(12)$$

wherein the friction factor f is found from Ref. 1., Fig. 85, p. 139. The density ρ_c is evaluated at the average temperature of the core, $(t_{2m} + t_1)/2$.

The pumping power is

$$P_P = \Delta p \frac{GA}{\rho_P} \qquad \ldots(13)$$

wherein the density ρ_P is evaluated at the pump. Δp is the total pressure drop of the system. A drop of 15 per cent of the drop through the core (Eq. 11) was added for acceleration (This was checked for several cases and found to be sufficiently accurate) and 1.5 psi was added for the heat exchanger and duct drops.

Plant heat balances were calculated for several outlet temperatures and combined with the gross thermal power of Eq. 9 above to give the gross electric power after allowance for plant auxiliaries but before deduction for coolant pumping power.

The mechanical pumping power from Eq. 13 was doubled, by an assumption of 50% efficiency from busbar to moving gas, to give the electric pumping power. This was then subtracted from the gross electric power to give the net electric output of the plant. The results are plotted in Fig. 5, 6, and 7.

Nomenclature

σ Average-to-peak ratio of radial neutron flux distribution.

$\frac{r}{R}$ Core radius normalized to 1 at outside.

$\phi\left(\frac{r}{R}\right)$ Radial flux distribution.

t_2 Temperature of coolant leaving a fuel channel.

t_1 Inlet temperature of coolant to the core.

t_f Limiting temperature in the uranium, assumed to be 1200° F in this case.

L Length of core from lower end of bottom-most fuel slug to upper end of topmost fuel slug.

L_o Extrapolated length of equivalent bare core.

λ Reactor Heat Transfer Number. Defined by $\lambda_c = \dfrac{\rho_c A_f v_c C_p}{\alpha UA}$

(Subscript c denotes center channel of the core)

ρ Coolant density

v Coolant velocity

A_f Coolant flow area, per channel.

C_p Coolant specific heat.

α Factor to allow for heat generation in the moderator. Taken to be 1.065 in this case.

(UA) Overall heat transfer coefficient from the center of the uranium to the coolant.

t_{2m} Mixed mean outlet temperature of the coolant.

$\Delta t'$ Expression defined by Eq. 4.

$f(\lambda_c)$ Function defined by Eq. 8.

(GA) Total mass flow through the reactor.

P_G Gross thermal power of the reactor.

G_c Mass flow through center channel.

Δp Pressure drop.

f Friction factor

g Gravitational acceleration.

D_e Hydraulic diameter of fuel channel containing a fuel element.

APPENDIX B

THERMODYNAMIC CYCLE CALCULATIONS

by
G. A. Schneider

I. REACTOR OUTPUT

Given: λ_c = 3.5 and CO_2 Pressure = 350 psi

CO_2 inlet temp. to steam generators = 650° F = T_1

CO_2 outlet temp. from steam generators = 300° F = T_2

CO_2 flow rate = 3477 lb CO_2/sec = W

Then reactor thermal output is

$$\frac{W\, C_p(T_1 - T_2)}{3.413 \times 10^6} = \frac{3477 \frac{\text{lb } CO_2}{\text{sec}} \times 3600 \frac{\text{sec}}{\text{hr}} \times .2525\ (650-300)}{3.413 \times 16^6}$$

$$= 327 \text{ thermal megawatts}$$

Total thermal power is

$$3477 \frac{\text{lb } CO_2}{\text{sec}} \times 3600 \frac{\text{sec}}{\text{hr}} \times .2525(650-300) = 1{,}108{,}000{,}000 \frac{\text{Btu}}{\text{hr}}$$

Total CO_2 flow rate is

$$3477 \frac{\text{lb } CO_2}{\text{sec}} \times 3600 \frac{\text{sec}}{\text{hr}} = 12{,}517{,}000 \text{ lb/hr } CO_2$$

II. STEAM SYSTEM

From previous heat balances, the optimum ratio of the heat absorbed by the high pressure steam cycle to the total heat absorbed by the steam plant is 0.57 to 1.0. The distribution of the heat to the high pressure steam cycle is

$$0.57 \times 1,108,000,000 = 630,800,000 \text{ Btu/hr}$$

while remainder 0.43 is to low pressure steam cycle; equals $0.43 \times 1,108,000,000 = 477,200,000$ Btu/hr.

Using a 45° F temperature difference between the inlet CO_2 gas temperature and the outlet high pressure steam temperature, the outlet high pressure steam temperature from the superheater equals 605° F. Allowing a 5° F temperature drop from the superheater outlet to the steam turbine inlet results in a steam turbine inlet temperature of 600° F. The corresponding turbine inlet pressure equals 340 psia from the turbine expansion line for this turbine shown on Fig. 13. Assuming a 30 psi pressure drop from the superheater outlet to the turbine inlet then gives a superheater outlet pressure of 370 psia.

The low pressure admission pressure to the turbine was selected to coincide with the expansion line of the high pressure steam cycle which is 60 psia. The corresponding temperature from Fig. 13 is 330° F. Allowing a 5° F temperature drop and a 15 psi pressure drop between the low pressure steam superheater and the low pressure steam admission point gives values of 75 psia and 335° F at the low pressure steam superheater outlet.

Heat absorbed by high pressure steam cycle is

$$Q_{\substack{\text{H.P. Mixed}\\ \text{Economizer}}} + Q_{\substack{\text{H.P.}\\ \text{Economizer}}} + Q_{\substack{\text{H.P.}\\ \text{Boiler}}} + Q_{\substack{\text{H.P.}\\ \text{Superheater}}} = Q_{\substack{\text{Total}\\ \text{H.P.}}} = 630,800,000 \text{ Btu/hr}$$

Letting y = Total pounds of high pressure steam and taking enthalpy differences across each heat exchanger, then:

$$\begin{array}{c}267.5\\ \underline{218.5}\\ (49.0)y\end{array} + \begin{array}{c}418.5\\ \underline{267.5}\\ (151.0)y\end{array} + \begin{array}{c}1204.3\\ \underline{418.5}\\ (785.8)y\end{array} + \begin{array}{c}1312.1\\ \underline{1204.3}\\ (107.8)y\end{array} = 630,800,000 \text{ Btu/hr}$$

$$1093.6y = 630,800,000 \text{ Btu/hr}$$

High Pressure Steam Flow = 577,000 lb/hr

Heat absorbed by low pressure steam cycle is

$$Q_{\substack{\text{L.P. Mixed}\\ \text{Economizer}}} + Q_{\substack{\text{L.P.}\\ \text{Boiler}}} + Q_{\substack{\text{L.P.}\\ \text{Superheater}}} = Q_{\substack{\text{Total}\\ \text{L.P.}}} = 477{,}200{,}000 \text{ Btu/hr}$$

Letting x = Total pounds of low pressure steam then:

$$\begin{array}{rrrl} 280.2 & 1182.6 & 1197.2 & \\ \underline{218.5} & \underline{280.6} & \underline{1182.6} & \\ (\ 61.7)x \ + & (\ 902.0)x \ + & (\quad 14.6)x & = 477{,}200{,}000 \text{ Btu/hr} \\ & & 978.3x & = 477{,}200{,}000 \text{ Btu/hr} \end{array}$$

Low Pressure Steam Flow = 488,000 lb/hr

Total Steam Generated = 577,000 + 488,000 = 1,065,000 lb/hr

With a CO_2 cold leg temperature of 300° F, the entering feedwater temperature to the heat exchangers was selected as 250° F. The condensate from the condenser at 2 in. back pressure is 101.1° F (69.1 h). The extraction steam flow from the turbine to preheat in a deaerating feedwater heater to 250° F (218.5 h) requires steam at 29.8 psia. Referring this pressure back to the turbine expansion line and assuming 5 per cent pressure drop from the turbine extraction bleed point, gives 31.4 psia at the extraction which corresponds to 1156 Btu/lb. (See Fig. 13). The extraction flow required x, is then

$$(577{,}000 + 488{,}000 \text{ lb/hr} - x)\, 69.1 + 1155x = 1{,}065{,}000\,(218.5)$$

$$\begin{array}{rcr} 73{,}600{,}000 - 69.1x + 1155.0x & = & 233{,}000{,}000 \\ \underline{69.1x} & & \underline{73{,}600{,}000} \\ 1085.9x & & 159{,}400{,}000 \end{array}$$

Extraction Flow x = 147,000 lb/hr

When calculating the kilowatt output of the steam turbine, the high pressure and low pressure steam flows were corrected for, by subtracting a proportional amount of steam used in feedwater preheating.

The amount of extraction steam apportioned to the high pressure cycle is then

$$\frac{577{,}000}{577{,}000 + 488{,}000} \times 147{,}000 = 80{,}000 \text{ lb/hr to H.P. cycle}$$

$$147{,}000 - 80{,}000 = 67{,}000 \text{ lb/hr to L.P. cycle}$$

III. ELECTRICAL GENERATION

Then Plant Generation Equals: (see Fig. 13 for enthalpy values)

$$577{,}000 \times \begin{array}{r} 1311.6 \\ 1156.0 \\ \hline 155.6 \end{array} = 90{,}000{,}000 \text{ Btu/hr}$$

$$\begin{array}{r} 577{,}000 \\ -80{,}000 \\ \hline 497{,}000 \end{array} \times \begin{array}{r} 1156 \\ 982 \\ \hline 174 \end{array} = 86{,}500{,}000 \text{ Btu/hr}$$

$$488{,}000 \times \begin{array}{r} 1197.8 \\ 1156.0 \\ \hline 41.8 \end{array} = 20{,}400{,}000$$

$$\begin{array}{r} 488{,}000 \\ -67{,}000 \\ \hline 421{,}000 \end{array} \quad \begin{array}{r} 1156 \\ 982 \\ \hline .174 \end{array} = \underline{73{,}300{,}000}$$

$$270{,}200{,}000 \text{ Btu/hr} \div 3413 = 79{,}300 \text{ kw}$$

Total power before losses	79,300 kw
Mechanical Loss	700 kw
Turbine Exhaust Loss $= \dfrac{(1{,}065{,}000-147{,}000)\text{x}15\text{x}.86}{3{,}413} =$	3,470 kw
Generator Loss 1.3%	930 kw
Gross Generation =	74,200 kw
4% Steam Plant Auxiliary Power =	2,950 kw
CO_2 Pumping Power	4,610 kw
Net Generation =	66,640 kw

Steam Plant Thermal Efficiency (including CO_2 pumping power) =

$$\frac{66{,}640 \times 3413}{577{,}000\,(1312.1 - 218.5) + 488{,}000\,(1210.6 - 218.5)} = 20.53\%$$

Steam plant thermal efficiency (excluding CO_2 pumping power) =

$$\frac{71{,}250 \times 3413}{577{,}000\,(1312.1 - 218.5) + 488{,}000\,(1210.6 - 218.5)} = 21.95\%$$

IV. HEAT ABSORPTION

Using the enthalpy values from Fig. 14 the heat absorbed by each heat exchanger is as follows:

Mixed high pressure economizer

$$577{,}000 \text{ lb/hr} \times \begin{array}{r} 267.5 \\ \underline{218.5} \\ 49.0 \end{array} = 28{,}100{,}000 \text{ Btu/hr}$$

High pressure economizer

$$577{,}000 \text{ lb/hr} \times \begin{array}{r} 418.5 \\ \underline{267.5} \\ 151.0 \end{array} = 87{,}150{,}000 \text{ Btu/hr}$$

High pressure boiler

$$577{,}000 \text{ lb/hr} \times \begin{array}{r} 1204.3 \\ \underline{418.5} \\ 785.8 \end{array} = 453{,}450{,}000 \text{ Btu/hr}$$

High pressure superheater

$$577{,}000 \text{ lb/hr} \times \begin{array}{r} 1312.1 \\ \underline{1204.3} \\ 107.8 \end{array} = \underline{62{,}100{,}000 \text{ Btu/hr}}$$

Total heat absorbed by high pressure steam cycle = 630,800,000 Btu/hr

---

Mixed low pressure economizer

$$488{,}000 \text{ lb/hr} \times \begin{array}{r} 280.2 \\ \underline{218.5} \\ 61.7 \end{array} = 30{,}100{,}000 \text{ Btu/hr}$$

Low pressure boiler

$$488{,}000 \text{ lb/hr} \times \begin{array}{r} 1182.6 \\ \underline{280.2} \\ 902.4 \end{array} = 440{,}000{,}000 \text{ Btu/hr}$$

Low pressure superheater

$$488{,}000 \text{ lb/hr} \times \begin{array}{r} 1197.2 \\ \underline{1182.6} \\ 14.6 \end{array} = \underline{7{,}100{,}000 \text{ Btu/hr}}$$

Total heat absorbed by low pressure steam cycle = 477,200,000 Btu/hr

Total heat absorbed by L.P. and H.P. Cycles = 477,200,000 + 630,800,000 =
= 1,108,000,000 Btu/hr

To solve for the outlet CO_2 gas temperatures from each heat exchanger, use average specific heat values across each exchanger and the heat absorbed by each exchanger; starting with the top heat exchanger, the high pressure superheater and working down to the bottom heat exchanger, the mixed high pressure economizer:

High pressure superheater

$$\underset{\text{Exchanger duty}}{62{,}100{,}000\ \text{Btu/hr}} = \underset{CO_2\ \text{Gas Flow}}{12{,}517{,}000\ \text{lb/hr}}\ \underset{\text{avg. } C_p}{(.2625)}\ \underset{\Delta t}{(650 - t_1)}$$

$$\frac{62{,}100{,}000}{12{,}517{,}000 \times .2625} = 650 - t_1$$

$$18.9 = 650 - t_1 \qquad t_1 = 631.1°\ F$$

High pressure boiler

$$453{,}450{,}000 = 12{,}517{,}000\ (.258)\ (631.1 - t_2)$$

$$140 = 631.1 - t_2 \qquad t_2 = 491.1°\ F$$

High pressure economizer

$$87{,}150{,}000 = 12{,}517{,}000\ (.2522)\ (491.1 - t_3)$$

$$27.5 = 491.1 - t_3 \qquad t_3 = 463.6°\ F$$

Low pressure superheater

$$7{,}100{,}000 = 12{,}517{,}000\ (.2512)\ (463.6 - t_4)$$

$$2.3 = 463.6 - t_4 \qquad t_4 = 461.3°\ F$$

Low pressure boiler

$$440{,}000{,}000 = 12{,}517{,}000\ (.2470)\ (461.3 - t_5)$$

$$142.1 = 461.3 - t_5 \qquad t_5 = 319.2°\ F$$

Low pressure mixed economizer

$$30{,}100{,}000 = 12{,}517{,}000\ (.2425)\ (319.2 - t_6)$$

$$9.9 = 319.2 - t_6 \qquad t_6 = 309.3°\ F$$

High pressure mixed economizer

$$28{,}100{,}000 = 12{,}517{,}000\ (.2419)\ (309.3 - t_7)$$

$$9.3 = 309.3 - t_7 \qquad t_7 = 300°\ F$$

V. HEAT EXCHANGER SURFACE CALCULATIONS

Using a tube with extended surface as shown on Drawing No. 11 with aluminum fins on steel tubes for the economizer and boiler heat exchangers, the fin area per linear foot of tubing is then:

$$\text{Fin area} = A_f = \frac{\pi}{4}(3.75^2 - 1.75^2) \times 2 \times \frac{7 \text{ fins}}{1 \text{ in.}} \times 12 \text{ in.} = 1452.0 \frac{\text{in.}^2}{\text{lin. ft}} = 10.1 \frac{\text{ft}^2}{\text{lin. ft}}$$

$$\text{Bare tube area} = A_o = \pi \times 1.75 \times 12 - \pi \times 1.75 \times .05 \times \frac{7 \text{ fins}}{\text{in.}} \times 12 \text{ in.}$$

$$= 42.8 \frac{\text{in.}^2}{\text{lin. ft}} = 0.298 \frac{\text{ft}^2}{\text{lin. ft}}$$

$$\text{Total surface of extended fins + bare tube} = 1494.8 \frac{\text{in.}^2}{\text{lin. ft}} = 10.398 \frac{\text{ft}^2}{\text{lin. ft}}$$

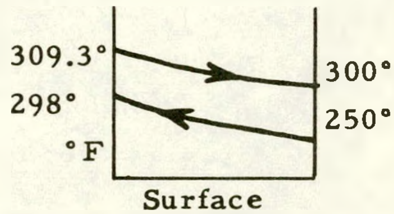
Total projected perimeter =

$$(2 \times 1 \text{ in.} \times 2 \times 7 \times 12) + 2\,(12 - 7 \times .050 \times 12) = 351.6 \frac{\text{in.}}{\text{lin. ft}}$$

$$d_e = \frac{2 \times \text{external surface}}{\pi \times \text{projected perimeter}} = \frac{2 \times 1494.8}{\pi \times 351.6} = 2.71 \text{ in.}$$

$$D_e = \frac{2.71}{12} = 0.226 \text{ ft}$$

1. HIGH PRESSURE ECONOMIZER



Refer to Fig. 15, Temperature-Surface Diagram

$$\text{LMTD} = \frac{(300-250) - (309.3-298)}{\log_e \frac{50}{11.3}} = 26.1° \text{ F}$$

$$S = \frac{t_2 - T_1}{T_1 - t_1} = \frac{298 - 250}{309.3 - 250} = .809$$

$$R = \frac{T_1 - T_2}{t_2 - t_1} = \frac{309.3 - 300}{298 - 250} = .194$$

F_T = 0.98 (from Ref. 11, p. 549)

Where F_T = Correction factor to LMTD for the true temperature difference in crossflow

$$\Delta t_{\text{LMTD Corrected}} = 26.1 \times .98 = 25.6° \text{ F}$$

Net free gas flow area based on a 12 ft x 12 ft duct =

$$A_s = (12 \times 12 \text{ in.})(12 \times 12 \text{ in.}) - \frac{12 \times 12}{4}(12 \text{ in.} \times 12 \times 1.75 \text{ in.}) - \frac{(12 \times 12)}{4} \times$$

$$(2 \times .05 \times 1 \text{ in.} \times 7 \times 12 \text{ in.} \times 12)$$

$$A_s = 8046 \text{ in.}^2 = 55.8 \text{ ft}^2$$

Hot Fluid-Duct Side

T_{hot} avg. = 305° F $\quad \mu = 1.49 \times .03314 = .0494$

$C_p = .2423$

$k = .0155$

$$G_s = \frac{W}{A_s} = \frac{12{,}517{,}000 \text{ lb } CO_2/\text{hr}}{4 \text{ heat exch./reactor} \times 55.8} = 56{,}100 \; \frac{\text{lb } CO_2}{\text{hr - ft}^2 \text{ free area}}$$

$$R_{es} = \frac{D_e G_s}{\mu} = \frac{0.226 \times 56{,}100}{.0494} = 257{,}000$$

j_f = 760 (Ref. 11, p. 555, Fig. 16, 18)

$$\text{where } j_f = \frac{h_f' De}{k} \frac{(Cu)}{(k)}^{-1/3}$$

$$h_f = j_f \frac{k}{D_e} \left(\frac{C_p \mu}{k}\right)^{.333} = 760 \; \frac{.0155}{.226} \left(\frac{.2433 \times .0494}{.0155}\right)^{.333} = 48.0$$

$$h_f' = h_f = 48.0$$

Cold Fluid-Tube Side

$$at' = \frac{1.42^2}{4} = 1.581 \text{ in.}^2 \qquad D = \frac{1.42}{12} = 0.1183 \text{ ft}$$

$$at = \frac{N_T \, at'}{144} = \frac{36 \times 1.581}{144} = .396 \text{ ft}^2$$

$$G_T = \frac{w}{A_t} = \frac{577{,}000 \text{ lb/hr}}{4 \times .396 \text{ ft}^2} = 364{,}000 \frac{\text{lb}}{\text{hr - ft}^2}$$

$$@t_{\text{cold avg.}} = 274° \text{ F} \quad \mu = 0.16 \times 2.42 = .387$$

$$k = .444$$

$$C_p = 1.021$$

$$h_i = \frac{k}{D} .023 \left(\frac{DG}{\mu}\right)^{.8} \left(\frac{C_p \mu}{k}\right)^{.4} = \frac{.444}{.1183} .023 \left(\frac{.1183 \times 364{,}000}{.387}\right)^{.8} \left(\frac{1.021 \times .387}{.444}\right)^{.4}$$

$$h_i = 899$$

$$h_i' = .85 \times h_i = 765$$

$$(r_e - r_b)\sqrt{\frac{h_f'}{ky_b}} = \left(\frac{1.875 \text{ in.} - .875 \text{ in.}}{12 \text{ in.}}\right)\sqrt{\frac{48}{120 \times .00208}} = 1.153$$

where k for aluminum = 120 Btu/hr - ft^2 - °F - ft

$$Y_b = \text{half fin thickness} = \frac{.05 \text{ in.}}{2 \times 12 \text{ in.}} = .00208 \text{ ft}$$

$$\frac{r_e}{r_b} = \frac{1.875}{.875} = 2.14$$ Then $\Omega = .615$ (Ref. 11, Fig. 16, 13a, p. 542)

$$A_i = \frac{1.42\pi}{12} = .371 \text{ ft}^2$$ inside surface/linear ft tube

$$h_{f_i'} = (\Omega A_f + A_o)\frac{h_f'}{A_i} = (.615 \times 10.1 + .298)\frac{48}{.371} = 843$$

$$U_{Di} = \frac{h_{fi}' \times h_i'}{h_{fi}' + h_i'} = \frac{843 \times 765}{843 + 765} = 402$$

Inside tube surface/bank = 36 tubes x 12 x .371 = 160.5 $\frac{\text{ft}^2}{\text{bank}}$

$$A_i = \frac{Q}{U_{Di} \times \Delta t} = \frac{28{,}100{,}000}{4 \times 402 \times 25.6} = 682 \text{ sq ft}$$

$\frac{682}{160.5}$ = 4.25 banks, Say 5 banks.

Gas Side Pressure Drop

Net Free Volume =

$$\left[12 \times 12 \times \frac{3.469}{12}\right] - \frac{1}{2}\left[(5 + 4)\ \frac{\pi}{4}\ \frac{1.75^2 \times 12}{144}\right] - \frac{1}{2}\left[(5 + 4)\ \frac{\pi}{4}\ (3.75^2 - 1.75^2)\right] \times$$

$$\frac{.05}{144} \times 7 \times 12 = 39.56$$

$$\text{Friction Surface} = \frac{1}{2}\ (5 + 4) \times 10.398\ \frac{ft^2}{lin.\ ft} \times 12 = 561\ ft^2$$

$$D'_{ev} = 4\frac{N.F.V.}{F.S} = 4 \times \frac{39.56}{561} = .282$$

$$\left(\frac{D'_{ev}}{S_t}\right)^{.4} = \left(\frac{.282}{.333}\right)^{.4} = .936$$

$$R_{es} = \frac{D'_{ev}\ G_s}{\mu} = \frac{.282 \times 56{,}100}{.0494} = 320{,}000$$

f = .00148 (Ref. 11, Fig. 16-18, p. 555)

$$\rho CO_2 \ @\ 765°R\ \&\ 350\ psi = 1.97\ \frac{lb}{ft^3}$$

$$S = \frac{\rho}{62.4} = \frac{1.97}{62.4} = .0315 \qquad \left(\frac{S_L}{S_T}\right)^{.6} = 1.0$$

$$L_p = \frac{5 \times 3.469}{12} = 1.445$$

$$\Delta P_{gas} = \frac{f\ G_s^2\ L_p}{5.22 \times 10^{10} \times D'_{ev} \times S \times \phi_s} \left(\frac{D'_{ev}}{S_T}\right)^{.4} \left(\frac{S_L}{S_T}\right)^{.6}$$

$$\Delta P_{gas} = \frac{.00148\ (56100)^2\ 1.445 \times .936 \times 1}{5.22 \times 10^{10} \times .282 \times .0315 \times 1} = 0.0136\ psi$$

Tube Side Pressure Drop

$$\Delta P_{\text{Tube side}} = \frac{f\ L_N\ G_T^{\ 2}}{5.22 \times 10^{10} \times D \times S \times \phi_s}$$

$G_T = 364{,}000$ $R_{et} = 111{,}200$ $f = .00014$ (Ref. 11, Fig. 26, p. 836)

$L = 12$ ft $N = 5$ banks

$$\Delta P_{\text{Tube side}} = \frac{.00014 \times 12 \times 5 \times (364{,}000)^2}{5.22 \times 10^{10} \times .1183 \times 1 \times 1} = 0.18 \text{ psi}$$

2. LOW PRESSURE ECONOMIZER

Similar calculations for the low pressure economizer result in:

$\Delta t_{\text{LMTD corrected}} = 25°$ F

$h'_f = 48.6$ $h'_i = 685$ $U_{Di} = 379$

$A_i = 794$ sq ft Total number of banks = 5

$\Delta P_{\text{gas side}} = .0138$ psi $\Delta P_{\text{Tube side}} = .133$ psi

3. HIGH PRESSURE ECONOMIZER

Calculations similar to those for previous economizers result in:

$\Delta t_{\text{LMTD corrected}} = 97.5°$ F

$h'_f = 52.6$ $h'_i = 900$ $U_{Di} = 889$

$A_i = 500$ sq ft Total number of banks = 4

$\Delta P_{\text{gas side}} = .0111$ psi $\Delta P_{\text{Tube side}} = 0.136$ psi

4. HIGH PRESSURE BOILER

$$\text{LMTD} = \frac{(631.1 - 439.6) - (491.1 - 439.6)}{\text{LOG}_e \frac{191.5}{51.6}}$$

LMTD = 108° F

Hot Fluid-Duct Side

$@T_{hot\ avg.} = 561°\ F,$ and 350 psi $\qquad \mu = 1.88 \times .03314 = .0622$

$C_p = .258$

$k = .023$

$$G_s = \frac{W}{A_s} = \frac{12{,}517{,}000}{4 \times 55.8} = 56{,}100\ lb/hr\text{-}ft^2$$

$$R_{es} = \frac{D_e G_s}{\mu} = \frac{.226\ 56{,}100}{.0622} = 204{,}000$$

$$j_f = 640$$

$$h_t = 640\ \frac{.023}{.226}\left(\frac{.258 \times .0622}{.023}\right)^{.333} = 57.7$$

$$h_f' = h_f = 57.7$$

$$(r_e - r_b)\sqrt{\frac{h_f'}{ky_b}} = .0834\sqrt{\frac{57.7}{120 \times .00208}} = 1.27$$

$$\frac{r_e}{r_b} = 2.14 \qquad \Omega = 0.56$$

$$h_{f_i}' = (\Omega A_f + A_o)\frac{h_f'}{A_i} = (.56 \times 10.1 + .298)\ \frac{57.7}{.371} = 911$$

Cold Fluid-Tube Side

Using $h_i' = 1500$ Btu/hr for steam

$$Di = \frac{911 \times 1500}{911 + 1500} = \frac{1{,}368{,}000}{2411} = 567$$

$$A_i = \frac{453{,}450{,}000}{4 \times 567 \times 108} = 1850$$

$$\frac{1850}{160.5} = 11.5,\ \text{Say 12 banks}$$

Gas Side Pressure Drop

$$\text{Net Free Volume} = 41.6 - \frac{11.5}{4.5}(.902) - \frac{11.5}{4.5}(1.135) = 36.39$$

$$\text{Friction Surface} = \frac{1}{2}(12 + 11)\ 10.398 \times 12 = 1435$$

$$D'_{ev} = 4 \times \frac{36.4}{1435} = 0.1014$$

$$\left(\frac{D'_{ev}}{S_T}\right)^{.4} = \left(\frac{0.1014}{.333}\right)^{.4} = .621$$

$$L_p = 12 \times \frac{3.469}{12} = 3.469$$

$$R'_{es} = \frac{D'_{ev}\ G_s}{\mu} = \frac{.1014 \times 56{,}100}{.0622} = 91{,}600$$

$$f = .0017 \qquad \rho = 1.43\ \frac{\text{lb}}{\text{ft}^3} \quad @\ 561°\ \text{F}$$

$$S = \frac{1.43}{62.4} = .0229$$

$$\Delta P_{Gas} = \frac{.0017\ (56{,}100)^2\ 3.469 \times .621}{5.22 \times 10^{10} \times .0229 \times .1014 \times 1} = .0955\ \text{psi}$$

5. LOW PRESSURE BOILER

Similar Calculations for the low pressure boiler result in:

LMTD = 50° F $\qquad h'_f = 50.2 \qquad h'_i = 1500$

A_i = 4010 sq ft $\qquad$ number of banks = 25

ΔP_{Gas} = .317 psi

6. HIGH PRESSURE SUPERHEATER

$$\text{LMTD} = \frac{(631.1-439.6) - (650-605)}{\log_e \frac{191.5}{45}} = 102°\ \text{F}$$

Using 2 in. OD tube .150 in. wall thickness on 3 in. x 3 in. pitch See Dwg. 11.

$$A_s = (12 \times 12)(12 \times 12) - \frac{12 \times 12}{3} \times 2 \times 12 \times 12 = 6912 \text{ in.}^2 = 48 \text{ ft}^2$$

Hot Fluid Duct Side

$$G_s = \frac{12{,}517{,}000}{4 \times 48} = 65{,}300 \text{ lb } CO_2/\text{hr-ft}^2 \text{ free area}$$

$$U_{Di} = 189.6 \times .35 = 66.4$$

$$A_i = \frac{62{,}100{,}000}{66.4 \times 4 \times 102} = 2300 \text{ sq ft}$$

$$\text{Inside tube surface/bank} = 48 \times 1.70\,\pi \times 12 \times 12 = 36{,}900 \text{ in.}^2$$

$$= 256 \text{ ft}^2/\text{bank}$$

$\frac{2300}{256} = 8.98$ Say, 9 banks of tubes.

Gas Side Pressure Drop

$$\text{Net Volume} = (12 \times 12 \times \frac{2.6}{12}) - \frac{1}{2}(9 + 8)\frac{\pi}{4}\frac{(2^2 \times 12)}{144} = 28.98$$

$$\text{Friction Surface} = \frac{1}{2}(9 + 8) \times \pi \times \frac{2 \times 12 \times 12}{144} = 53.3$$

$$D'_{ev} = 4 \times \frac{28.98}{53.3} = 2.17$$

$$\left(\frac{D'_{ev}}{S_T}\right)^{.4} = \left(\frac{2.17}{.250}\right)^{.4} = 2.37$$

$$L_p = 9 \times \frac{2.60}{12} = 1.95$$

$$@\ 640°\ F \quad \mu = 1.98 \times .03314 = .0656$$

$$\rho = 1.34 \qquad S = \frac{1.34}{62.4} = .0215$$

$$R'_{es} = \frac{D'_{ev}\ G_s}{\mu} = \frac{2.17 \times 65{,}300}{.0656} = 2{,}160{,}000 \qquad f = .0011$$

$$\Delta P_{Gas} = \frac{.0011\ (65{,}300)^2\ 1.95 \times 2.37 \times 1}{5.22 \times 10^{10} \times .0215 \times 2.17 \times 1} \times .00891 \text{ psi}$$

Tube Side Pressure Drop

$$at' = \frac{1.70^2 \pi}{4} = 2.27 \text{ in.}^2 \qquad D = \frac{1.70}{12} = .142 \text{ ft.}$$

$$at = \frac{48 \times 2.27}{144} = .756 \text{ ft}^2 \qquad \rho = \frac{1}{1.262} = .793$$

$$G_T = \frac{577{,}000}{4 \times .756} = 191{,}000 \qquad S = \frac{.793}{62.4} = .0127$$

$\mu = .0469$

$f = .00010$

$$R_{et} = \frac{\frac{1.70}{.2} \times 191{,}000}{.0469} = 578{,}000$$

$$\Delta P_{tube\ side} = \frac{.00010\ (191{,}000)^2\ 12 \times 9}{5.22 \times 10^{10} \times .142 \times .0127 \times 1} = 4.2 \text{ psi}$$

Assume 10 psi drop including inter-connecting piping.

7. LOW PRESSURE SUPERHEATER

Similar calculations for the low pressure superheater result in:

LMTD = 140° F $\qquad U_{Di} = 47.5$

A_i = 268 sq ft $\qquad$ Total number of banks = 2

$\Delta P_{Gas\ side}$ = .0004 psi $\qquad \Delta P_{Tube\ side}$ = 3.03 psi

VI. TABULATION OF HEAT EXCHANGER DATA

Heat Exchanger	Number of Tube Banks	ΔP Gas Side	Outside Surface	Inside Surface	Linear Feet
H. P. SupH'tr.	9	.0089	2,709	2,305	5,190
H. P. Boiler	12	.0955	53,916	1,925	5,190
H. P. Econom.	4	.0111	17,972	642	1,730
L. P. SupH'tr.	2	.0005	601	512	1,152
L. P. Boiler	25	.3170	113,325	4,010	10,800
L. P. Econom.	5	.0138	22,465	8,025	2,160
H. P. Mixed Econ.	5	.0136	22,465	8,025	2,160
Total	62	.4604	233,453	25,444	28,382

Econ. and Boiler Tubes: External surface = 10.398 x 12 x 36 = 4493 ft^2/bank

Internal tube surface = .371 ft^2/lin. x 12 x 36 = 160.5 ft^2/bank

Linear ft/bank = 36 x 12 = 432

Bare tube surface/bank = $\frac{1.75\pi \times 12}{144} \times 12 \times 36 =$ 197.8 ft^2/bank

Superheater Tubes:

Inside tube surface = 48 x 1.7π = 256 ft^2/bank

Outside tube surface = 48 x 2π = 301 ft^2/bank

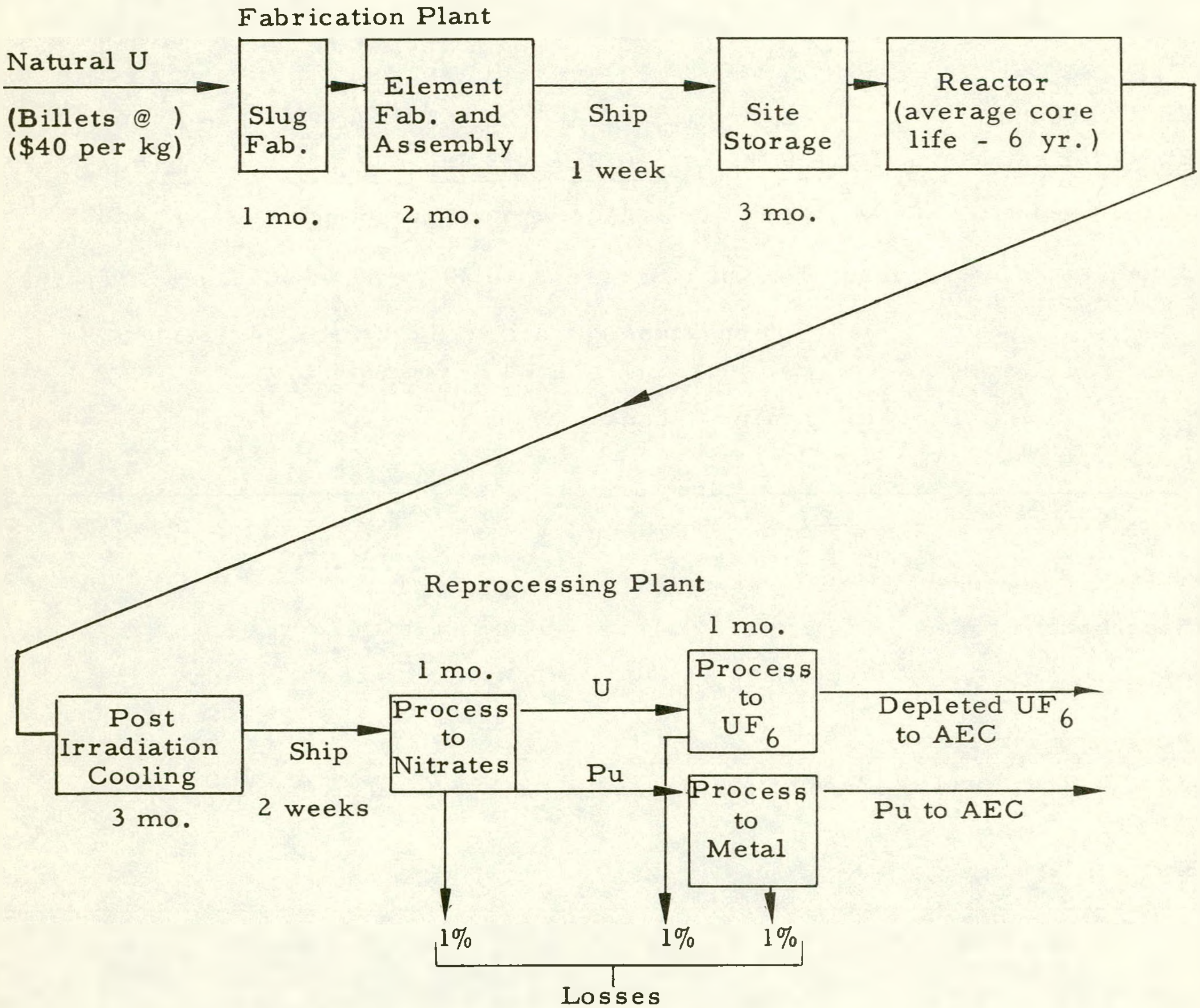
Linear ft/bank = 48 x 12 = 576 lin. ft/bank

APPENDIX C

ECONOMIC CALCULATIONS

by
W. T. Morgan

1. Steady-state Fuel Cycle



Total hold-up, exclusive of core - 11 months, 3 weeks, say 1 year.

2. Estimated Plant Costs

Item	Calder-Hall Stage "O" if Built in USA (millions)	Differences, Reference Design	Cost, Reference Design (millions)
Reactor & Bldg.	$14.28	Larger Physically	$17.0
Steam Generators	5.66	Smaller; Different Construction	2.0
Gas Circulators	2.27	About 2/3 Size	1.7
Circ. Loops	.31	Higher Pressure	.6
Stacks	.02	Same	.02
Feed Pumps	.07	Larger	.08
Circ. Pumps	.07	Larger	.08
Fuel Handling	.26	Refuel Under Gas Pressure	.40
Inst. & Control	3.15	Slightly Larger	3.2
Piping	9.52	Same	9.5
Engrg. & Overhead	14.12		10.0
Total - Reactor Plant	49.73		44.58
Turbogen. & Aux.	7.70	(Sized up from 92 MW Gross to 148 MW Gross)	9.5
Condensers & Aux.	.62		1.0
Feedwater Systems	.26		.4
Inst. & Electrical	.57		.8
Piping	2.80		3.8
Engr. & Overhead	3.15		3.0
Total - Turbogen. Plant	15.10		18.5
Buildings and Site	4.18		4.5
TOTAL	$69.01		$67.58

3. <u>Economic Calculations (for two reactors)</u>

a. Fuel feed rate (2 reactors) $= 2 \times \dfrac{327 \text{ TMW} \times 0.6 \text{ P.F.} \times 365 \text{ days/year}}{3{,}000 \text{ TMWD/tonne}}$ *

$= \underline{\underline{47.6}}$ tonne/year $= \underline{\underline{105{,}200}}$ lb/year

b. $\dfrac{\text{Fuel feed rate}}{\text{Core loading}} = \dfrac{23.8 \text{ tonne/year}}{140.7 \text{ tonne}} = 16.9\%$ per year

Average core lifetime $= \dfrac{1}{16.9\%} = \underline{\underline{5.9}}$ years

c. Fuel elements to be changed $= 2 \times \dfrac{52{,}600 \text{ lb/year}}{28.33 \text{ lb/element}}$

= 3,720 elements/year

d. Fuel lease charge = 2 x 4% x \$40/kg x (core loading and process hold-up)

= 2 x 4% x \$40 x (140.7 and 23.8 tonne)

= $\underline{\underline{\$528{,}000}}$ per year

e. Plant output 1 $= 130{,}000 \text{ kw} \times 8760 \dfrac{\text{hours}}{\text{year}} \times 0.6 \text{ P.F.} = \underline{\underline{683 \times 10^6}}$ kwh/year

Plant output 2 $= 130{,}000 \text{ kw} \times 8760 \times 0.85 \text{ P.F.} = \underline{\underline{965 \times 10^6}}$ kwh/year

* TMW = thermal megawatt
P.F. = plant factor
tonne = 1,000 kilograms = 2204.6 lb.

APPENDIX D
CONTROL SYSTEM DESCRIPTION

by
E. B. Ash

I. REACTOR AND STEAM PLANT CONTROL SYSTEM

The control system is designed for a "load following" plant, meaning that the power level of the plant is varied by changing the heat extracted (steam flow rate) and the heat generated automatically follows the demand; thus the demand from the electrical portion of the plant controls the net power output, and the reactor power level automatically follows to supply the power demanded by the electrical load.

Because of the high capital cost of nuclear power plants, base load operation is desirable; however, to allow flexibility the control system is designed to allow the plant power output to be varied at reasonable rates. With this in mind the following objectives were set for the control of the system temperatures and pressures.

1. Reactor temperatures remain constant for all operating power levels. This is required for several reasons:
    a. To minimize transients.
    b. Because of the stored heat in the graphite and structures system, temperatures can not be changed rapidly. This would limit the rate of power change.
    c. Reduced flow rates at low loads means lower pumping costs and improved plant efficiency.
2. Steam pressure remains constant in the high pressure steam generator. Care is required to control steam pressure since with constant reactor temperatures it tends to rise at low loads.
3. Steam pressure is variable in the low pressure steam generator. Because of the type of control used, the pressure in this steam generator rises at reduced loads. The design pressure in this unit must be the same as for the high pressure unit for this reason.

The plant control scheme is shown in Dwg. 16. It consists of several individual controllers. These are:

1. <u>Neutron Flux Controller</u> - This controller regulates the reactor power level by means of the regulating rod to return the neutron flux to the set point. The set point consists of a steam flow "programming" signal, and a reset signal which returns the reactor outlet temperature to its design value. The steam flow signal acts as an anticipation signal, and

adjusts the reactor power level to quickly match the steam demand. The temperature reset signal "trims" the power level to the design outlet temperature.

2. Gas Flow Controller - This controller changes the CO_2 flow rate in each loop to match the set point. There are four of these controllers having a common set point thereby assuring equal flows in each loop. To control the gas flow rate, the power to each of the gas circulators is varied by a motor-generator set and a Ward-Leonard controller. The set point from the gas flow controller is obtained from the combination of a steam flow "programming" signal plus a pressure reset signal, the pressure signal being obtained from the pressure in the common header to the turbine. Thus, the gas flow controller serves to closely match and control the flow rates in each loop so that the pressure in the high pressure steam generator remains the same.

3. Low Pressure Steam Generator Flow Control - This controller serves to change the amount of steam flow from the low pressure steam generator and thus hold the gas temperature returning to the reactor constant. This is done by controlling the position of a throttling valve in the low pressure steam generator line. The position of this valve is programmed by a steam flow signal (for anticipation) and is reset by gas temperatures returning to the reactor. Thus, the percentage of heat extracted in the low pressure steam generator will be reduced at low loads to hold the reactor inlet temperature constant.

4. Steam Dump Controller - A steam dump condenser is provided to dispose of the full power output of the plant into the condenser during emergency conditions. It is also useful during load changes when steam pressure may rise slightly during a transient. This controller is actuated from a pressure signal taken from the common steam header and actuates a dump valve to hold this pressure constant. One controller is required for the low pressure steam, and one for the high pressure steam.

5. Other Steam Plant Controllers - Other controllers are required for the operation of the steam plant. These are level controllers, pressure controllers, feed water controllers, etc. These are all conventional steam plant controllers and are not described.

To minimize temperature transients which reduce the life of the plant the control system uses anticipation signals to improve the transient performance of the plant. All critical controllers (such as neutron flux and low pressure steam generation) have three-action control (proportional, rate, and reset) to optimize the transient response of the plant and thus minimize temperature and pressure transients.

The use of a low pressure steam generator not only improves the efficiency of the plant at full load but it enables the reactor temperatures to be held constant, and still maintain the pressure in the high pressure steam generator constant.

II. REACTOR SAFETY SYSTEM

The control system just described is that which maintains the plant temperatures and pressures at their design values during normal operation. The safety system is designed to prevent transients and yet be reliable enough to minimize the number of unnecessary shutdowns. It takes the following actions in the event of an abnormal condition:

1. Alarm - An indication presented to the operator when some variable in the system has changed appreciably from its normal operating value. The operator must acknowledge the condition and may presumably correct for it by manually restoring the plant to its normal operation condition. The alarm system is conventional and will not be described.
2. Setback - If the variable continues to digress beyond the alarm point, the next action taken is to drive all the shim rods in at their maximum rate. This reduces the power level of the reactor quickly and should correct the abnormal condition. After the abnormal condition has been corrected, the setback circuits will automatically shut off.
3. Scram - If the variable continues to deviate beyond the setback point a scram will occur and will drop all the rods into the core to reduce the power level as rapidly as possible. To minimize thermal stresses, the CO_2 flow is also cut off and dynamically braked, and the turbine tripped simultaneous with a scram. Steam is then dumped into the condenser.

The scram and setback circuit block diagram is shown in Dwg. 15.

The following abnormal conditions will cause scrams and setbacks with the described consequences:

Scrams:

1. High neutron flux from two of three uncompensated ionization chambers. This scram indicates the power level has exceeded its design value. These scrams have a very fast response time and can cause a scram in a sufficiently short time to prevent large temperature excursions. The coincidence feature increases the reliability of this type of scram, because it requires two simultaneous signals to scram the reactor.
2. Short neutron period from one of two channels. These scrams are automatically cut out above 1 per cent of full power and are used primarily for protection during start-up.
3. Low level flux scrams are obtained from the log N channels and are used for protection during core loading procedures. These will be cut out at high power levels.
4. High coolant outlet temperature from two out of four loops. This is a relatively slow scram because of the time constants associated with the thermocouples. However, it is intended for a back-up to Overpower and Loss-of-flow scrams. Requiring two coincident signals to scram improves system reliability.
5. Loss of CO_2 pressure indicating a large leak somewhere in the system. This is an indication of an impending loss of cooling condition.
6. Manual scram.
7. High power (neutron flux) to coolant flow ratio. This scram is obtained by comparing the neutron flux signal with the total gas flow rate. If this ratio is excessively large it indicates an unbalance between power production and power extraction. This means either the power is too high or the flow rate is too low. Either condition is serious and if the ratio becomes too high the power must be rapidly reduced.

Setbacks: In general, only conditions which can be corrected by rapidly reducing the power level will activate the setback circuits. These conditions are:

1. Loss of flow from any one of the four coolant loops will trip all other loops simultaneously with the setback circuits. This prevents the gas circulators which have not failed from producing backflow in the loop

which has failed. The circulators will then coast to a stop and the rods will be driven in to reduce the reactor power level at a rate comparable to the flow coast down. This is a milder condition than a full scram, since a scram would reduce power level very rapidly and could produce large thermal stresses. If for some reason the power after setback were not reduced rapidly enough, the high power to flow ratio scram would more quickly reduce the power level by a full reactor scram.

2. High coolant outlet temperature from two of four loops.
3. High neutron flux from two of three signals from uncompensated high level chamber.
4. Short neutron period from either of two channels. These setbacks will not be cut out at high power levels as the period scrams are.
5. Manual setback.
6. High power level to coolant flow ratio.

In general, all setback signals will remove the reactor from automatic control and drive all the shim-safety rods in at a constant rate. This rate will be fast enough to quickly correct the faulty condition, and after it has been corrected the rods will stop automatically and the operator must return the reactor to automatic control.

III. REACTOR KINETIC BEHAVIOR

The reactor itself presents no particular control problems as far as kinetics is concerned. Its prompt neutron lifetime is expected to be in the order of one millisecond, much longer than many smaller-sized enriched reactors. The reactor is expected to have a total negative temperature coefficient of about $-2 \times 10^{-5}/°C$. The coefficient associated with the fuel temperature should be about $-0.5 \times 10^{-5}/°C$ and the moderator about $-1.5 \times 10^{-5}/°C$. This temperature coefficient of the fuel is a ralatively prompt effect since the stored heat in the uranium is considerably less than in the graphite. This prompt negative temperature coefficient associated with the fuel temperature should improve the self-regulation of the reactor and make the control easier. No great control problems have been associated with the control of reactors of this type.

In changing power levels in this plant, the graphite temperatures remain relatively constant since the coolant temperatures will remain the same at all loads. This decreases the amount of reactivity needed to change from one power level to another. However, as the power level of the reactor changes, the uranium temperatures must change accordingly. If the uranium temperatures change about 500° F from low loads to full load, less than 0.2 per cent reactivity is necessary. An adequate amount is contained in the regulating rod to make this change. However, any change in graphite temperature will increase the amount of reactivity necessary during power changes.

The shim rods must be moved during the following conditions:

1. Approaching criticality from shutdowns.
2. Plant heat-up (possibly). About 0.6 per cent reactivity is necessary to bring the plant from room temperature to operating temperature. This is slightly more than is contained in the regulating rods.
3. Poison buildup (xenon and samarium) during operation. This requires about 3.45 per cent reactivity.
4. Burnup. This requires about 1.5 per cent reactivity.

The shim rod rate quoted for group operation is fast enough to override any of the above effects very rapidly.

REFERENCES

1. J. G. Knudsen and D. L. Katz, Fluid Dynamics & Heat Transfer, University of Michigan Press, 1953.
2. J. G. Knudsen and D. L. Katz, "Heat Transfer & Pressure Drop in Annuli" Chemical Engineering Progress, October 1950, Page 490.
3. Nucleonics, September 1956, Page 10.
4. B. L. Goodlet and R. V. Moore, "Natural Uranium Power Reactor Design Study - Summary Report", AERE R/R 1955, 1956.
5. E. A. Guggenheim and M. H. L. Pryce, "A Quantitative Study of Uranium-Graphite Lattices", AERE R/R 922, 1952.
6. T. T. Shimazaki and C. H. Robbins, "Power Conversion in Nuclear Power Plants", NAA-SR-278, 1954.
7. Hilsenrath et. al., Tables of Thermal Properties of Gases, National Bureau of Standards Circular 564, U. S. Government Printing Office, 1955.
8. K. Jay, Calder Hall, Harcourt, Brace and Company, Inc., New York, 1956.
9. O. A. Hougen and K. M. Watson, Chemical Process Principles, Part 2, Page 712.
10. British Nuclear Energy Conference, "Symposium on Calder Works Nuclear Plant", 1956.
11. D. Kern, Process Heat Transfer, McGraw Hill, 1950.
12. J. K. Salisbury, Steam Turbines & Their Cycles, Wiley, 1950.
13. W. McAdams, Heat Transmission, McGraw Hill, 1954.
14. M. Jacob, Heat Transfer, Wiley, 1955.
15. S. Glasstone, Principles of Nuclear Reactor Engineering, Van Nostrand, 1955.
16. J. Keenan and F. Keyes, Thermodynamic Properties of Steam, Wiley, 1955
17. Nuclear Power, October 1956.
18. "Comparison of Calder Hall & PWR Reactor Types", American Radiator and Standard Sanitary Corporation, Report S-1.
19. "Initial Report on Nuclear Power Plant Economics", California Research and Development Company, Report MTA-34.
20. "Ninth Steam Station Cost Survey", Electrical World, October 17, 1945.
21. "Annual Report on Construction Costs", Engineering News-Record, September 29, 1955.
22. "State Commission Jurisdiction and Regulation of Electric and Gas Utilities", Federal Power Commission Report FPC S-113, June 1954.
23. Nucleonics, December 1956, Page S-13.
24. W. W. Davis, "Capital Investment for Nuclear Energy", Geneva Conference Paper P/477, 1955.

REFERENCES (Cont.)

25. H. K. Hardy, "Metallurgical Research and Development for Nuclear Power", J. of Institute of Metals, Vol. 84, pp. 229-238, 1956.

26. J. G. Ball, "Metallurgical Research in Nuclear Power Production", J of Institute of Metals, Vol. 84, pp 239-250, 1956.

27. Materials and Methods, December 1956, p 139.

28. A. B. McIntosh and K. Q. Bagley, "Selection of Canning Materials for Reactors Cooled by Sodium/Potassium and Carbon Dioxide", J. of Institute of Metals, Vol. 84, pp. 251-270, 1956.

29. MIT-5003, "Power Plants with Thermal Reactors", Project Dynamo

30. Hearings before the Joint Committee on Atomic Energy, Comments on Report of the Panel on the Impact of the Peaceful Uses of Atomic Energy (McKinney Panel), Part 2; pp. 582-586.

31. "Background Material for the McKinney Panel Report", Volume 2 Joint Committee Print, ;84th Congress, 2nd Session, U. S. Government Printing Office, 1956.

32. E. L. Grant, Principles of Engineering Economy, Ronald Press Co., 1938.

33. Forbes Magazine, January 1, 1957.

34. ANL-5036, Metallurgical Division Progress Report.

35. ANL-5234, Metallurgical Division Progress Report.

36. ANL-5257, Metallurgical Division Progress Report.

37. TID-7515, Part II, Minutes of Radiation Damage Meeting, 1956.

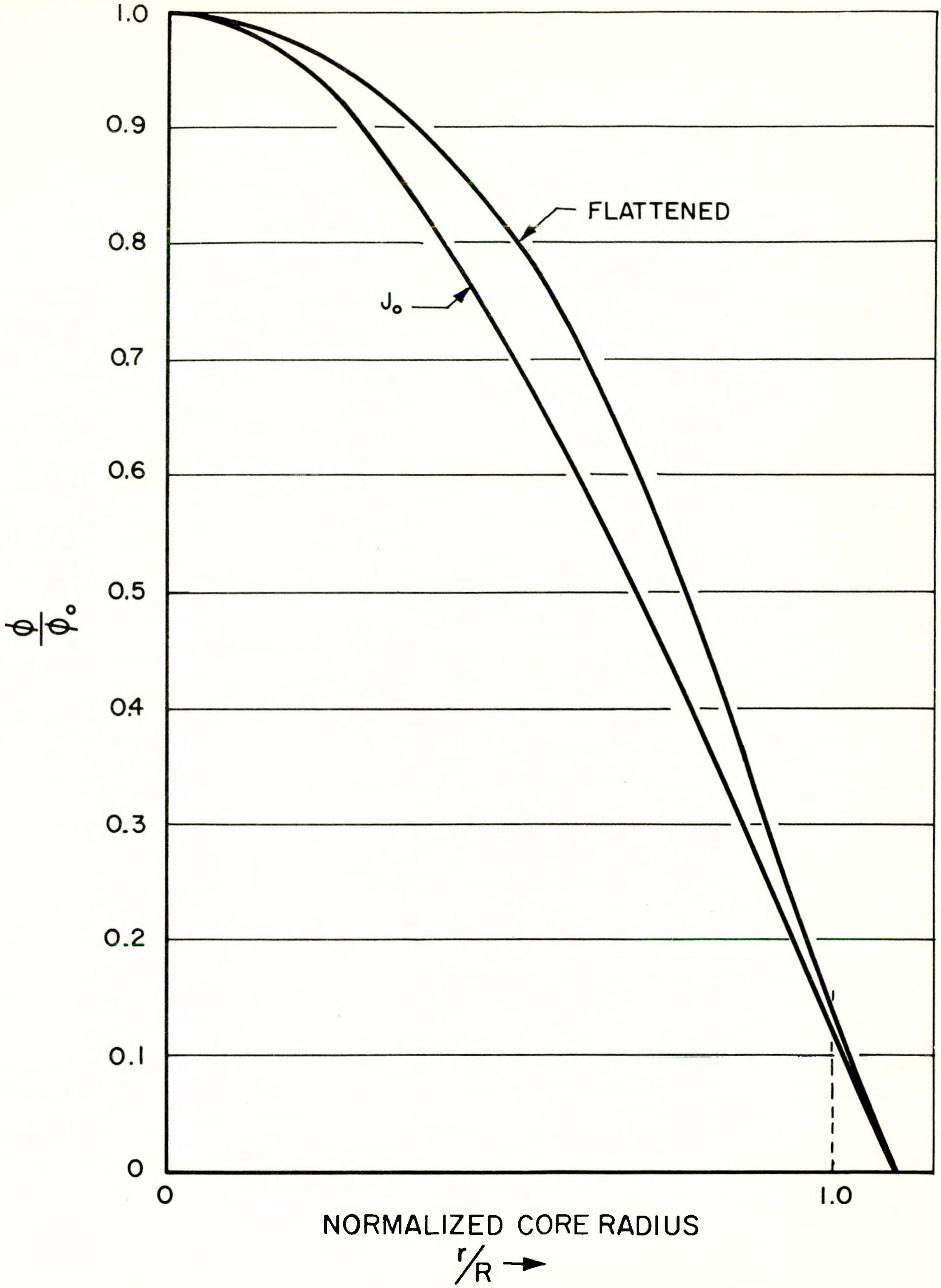


Fig. 1. Radial Variation in Thermal Neutron Flux

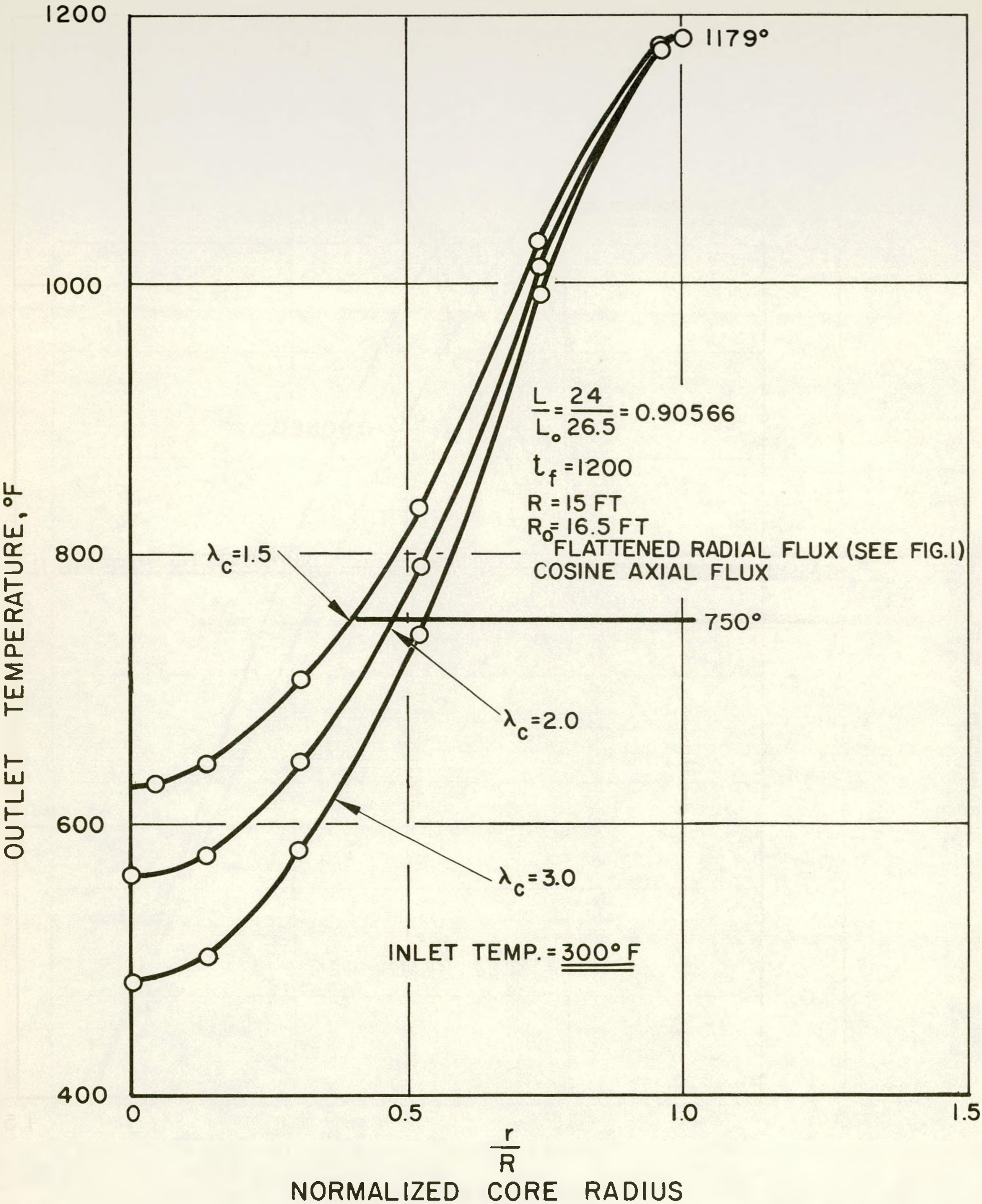


Fig. 2. Radial Outlet Temperature Distribution, Inlet Temperature 300° F

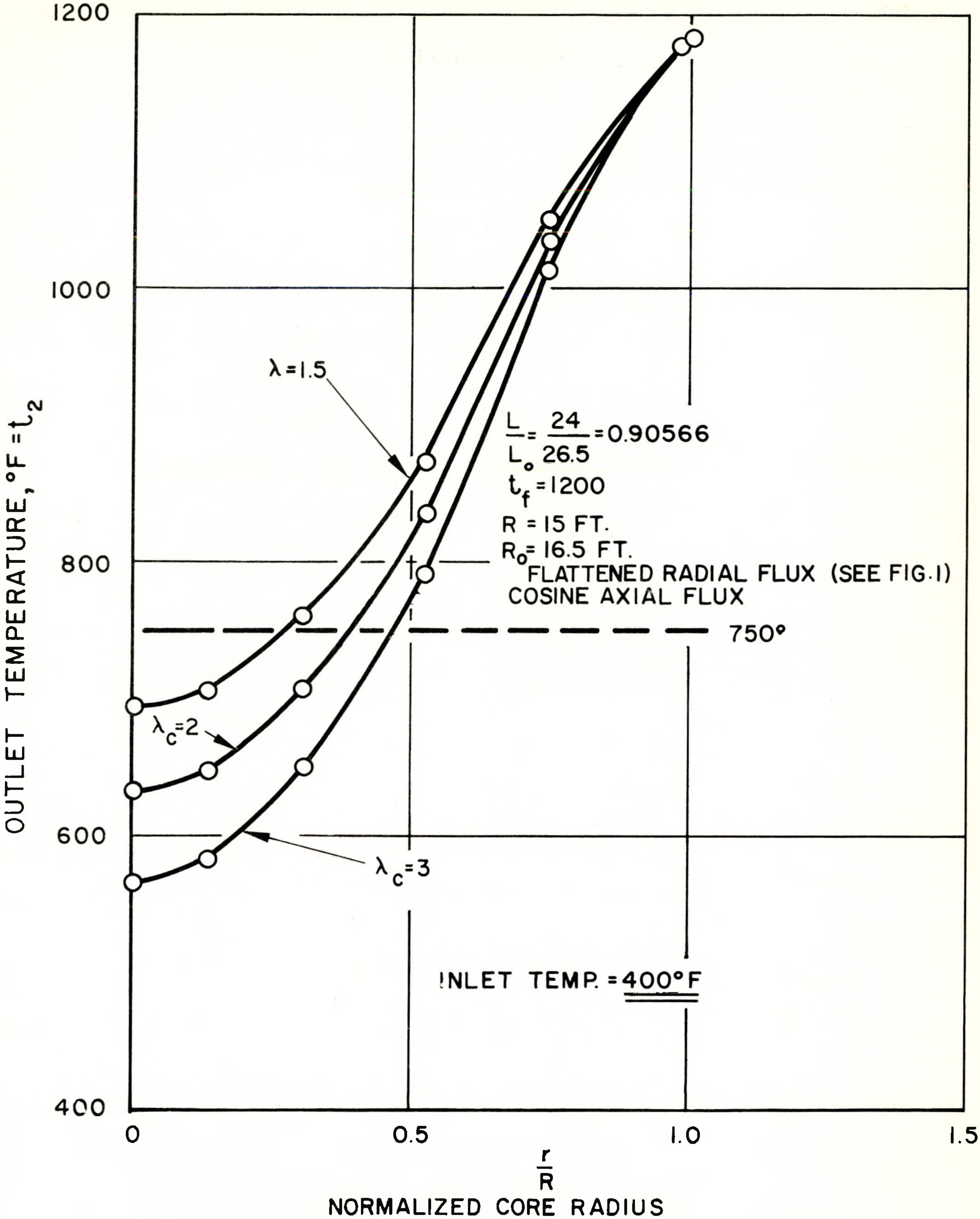


Fig. 3. Radial Outlet Temperature Distribution, Inlet Temperature 400° F

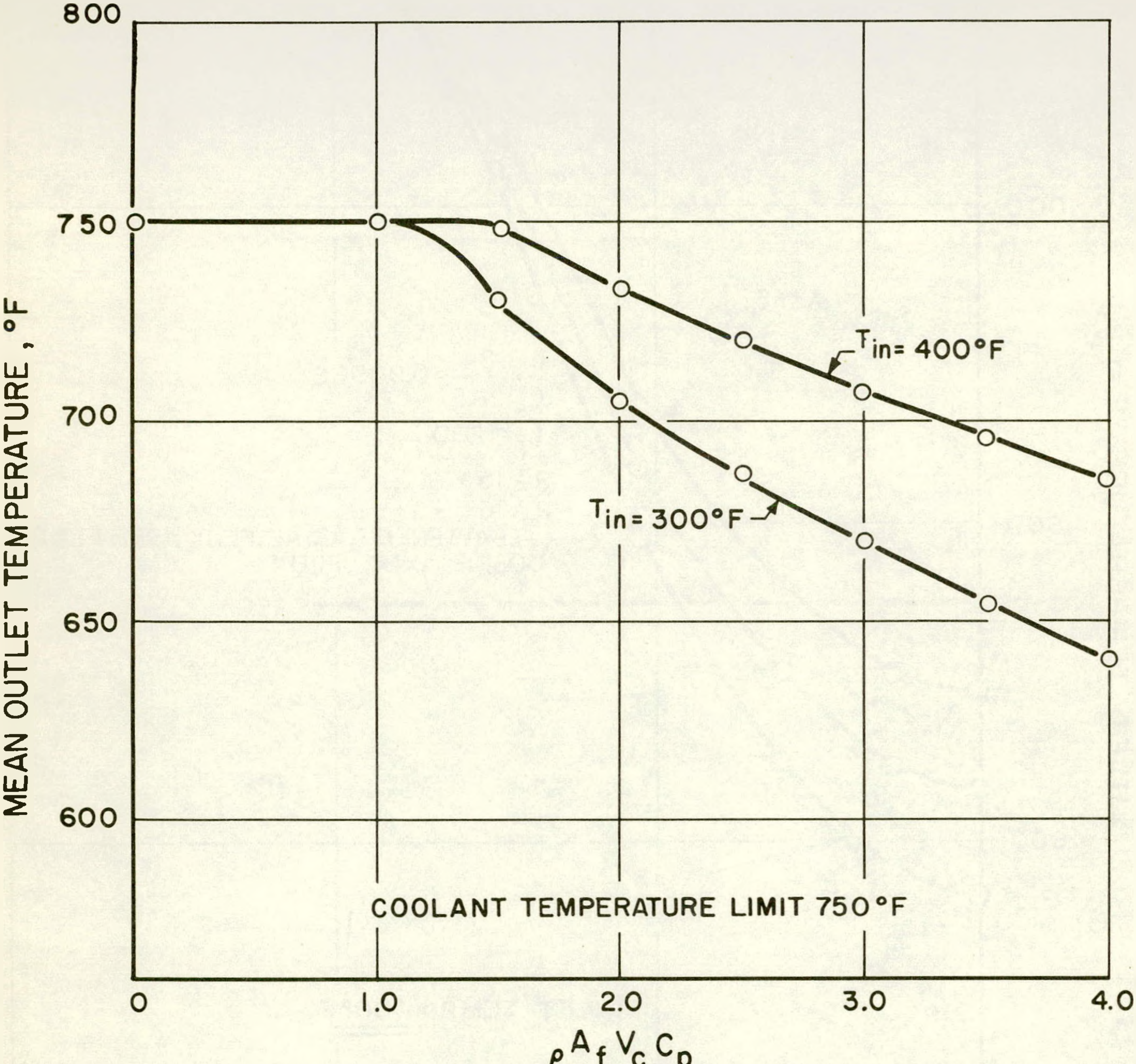


Fig. 4. Mean Outlet Temperature, Coolant Temperature Limit 750° F

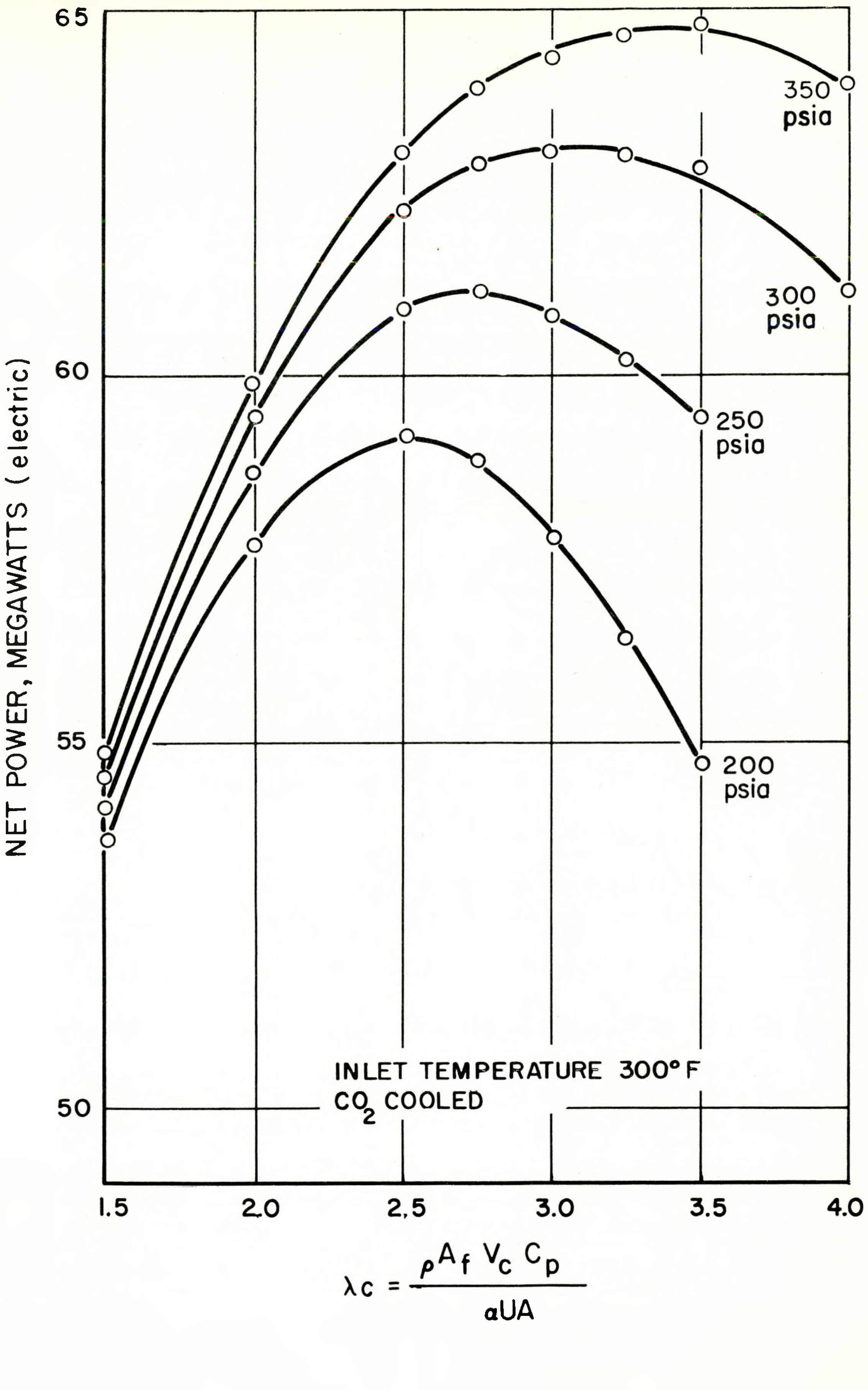


Fig. 5. Net Electric Power, Inlet Temperature 300° F, CO_2 Cooled

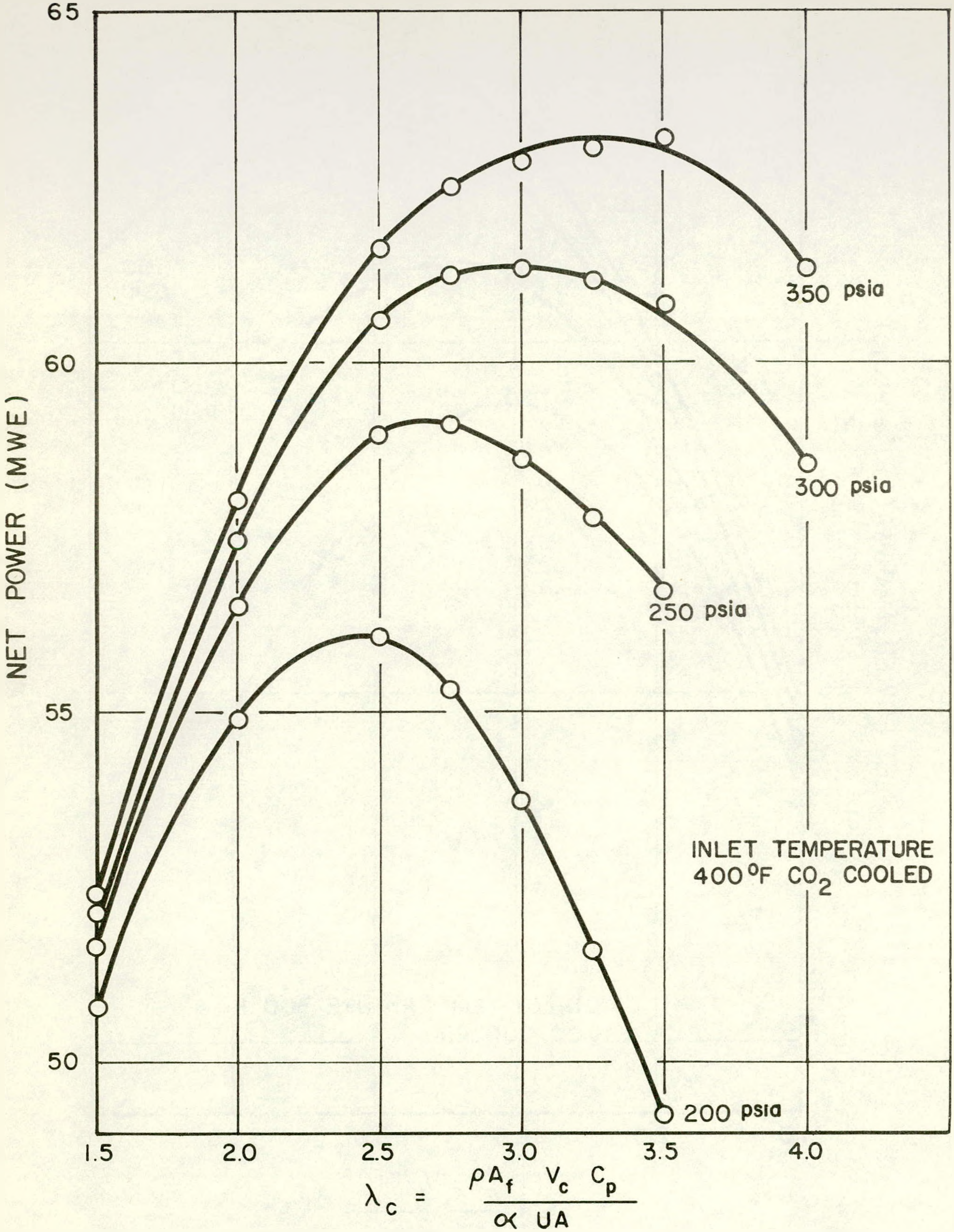


Fig. 6. Net Electric Power, Inlet Temperature 400° F, CO_2 Cooled

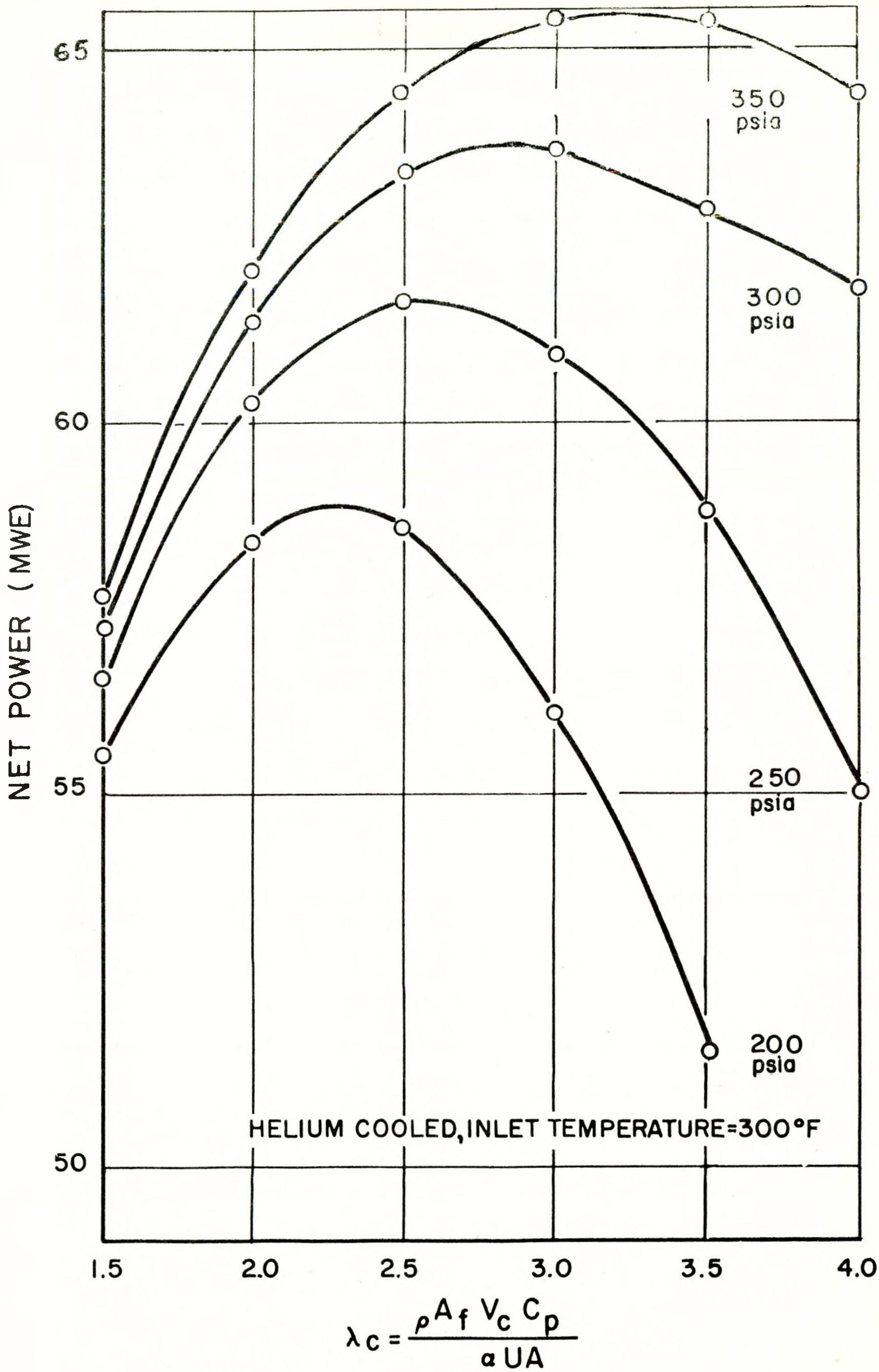


Fig. 7. Net Electric Power, Inlet Temp. 300° F

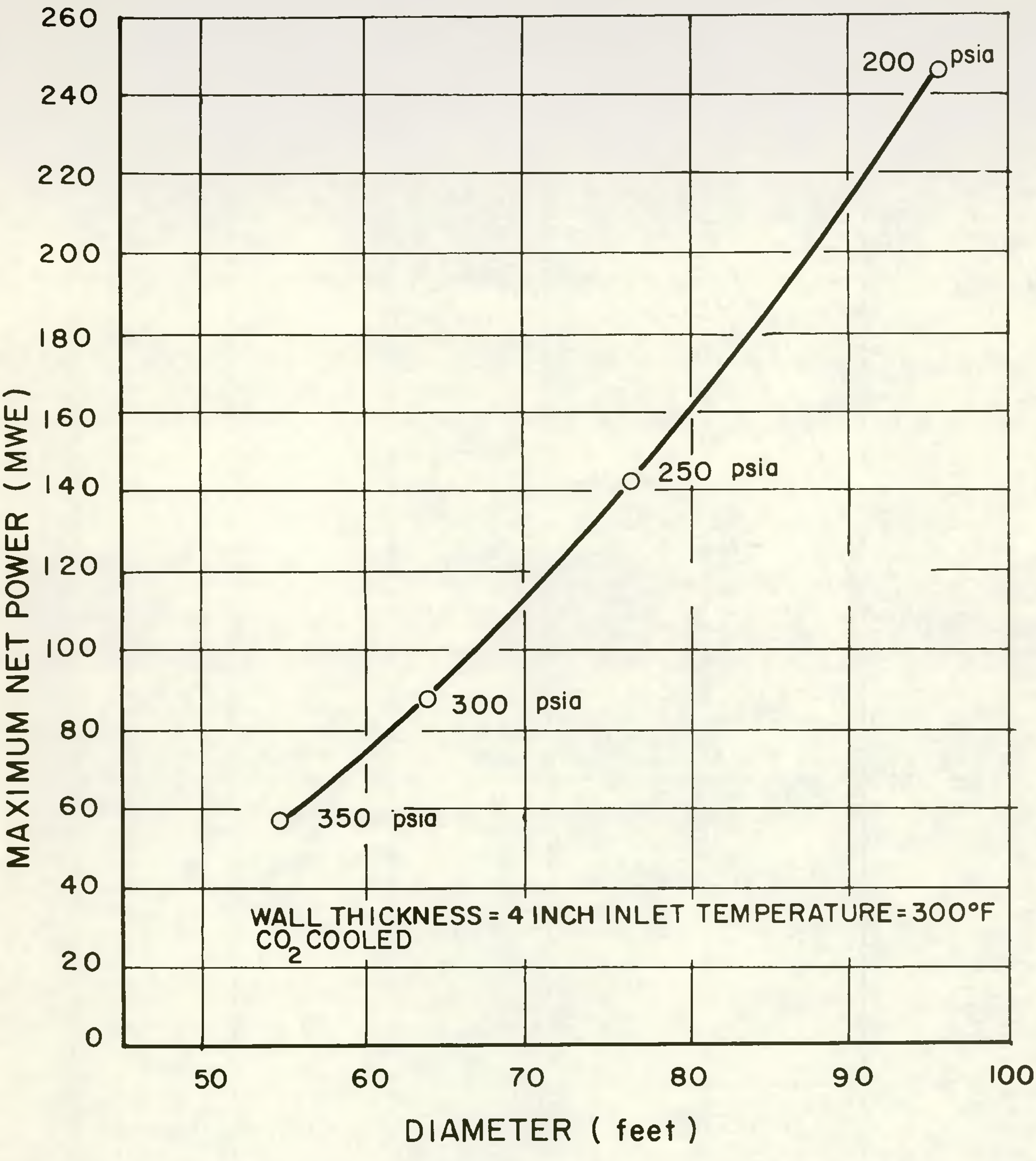


Fig. 8. Variation of Maximum Net Power With Vessel Diameter

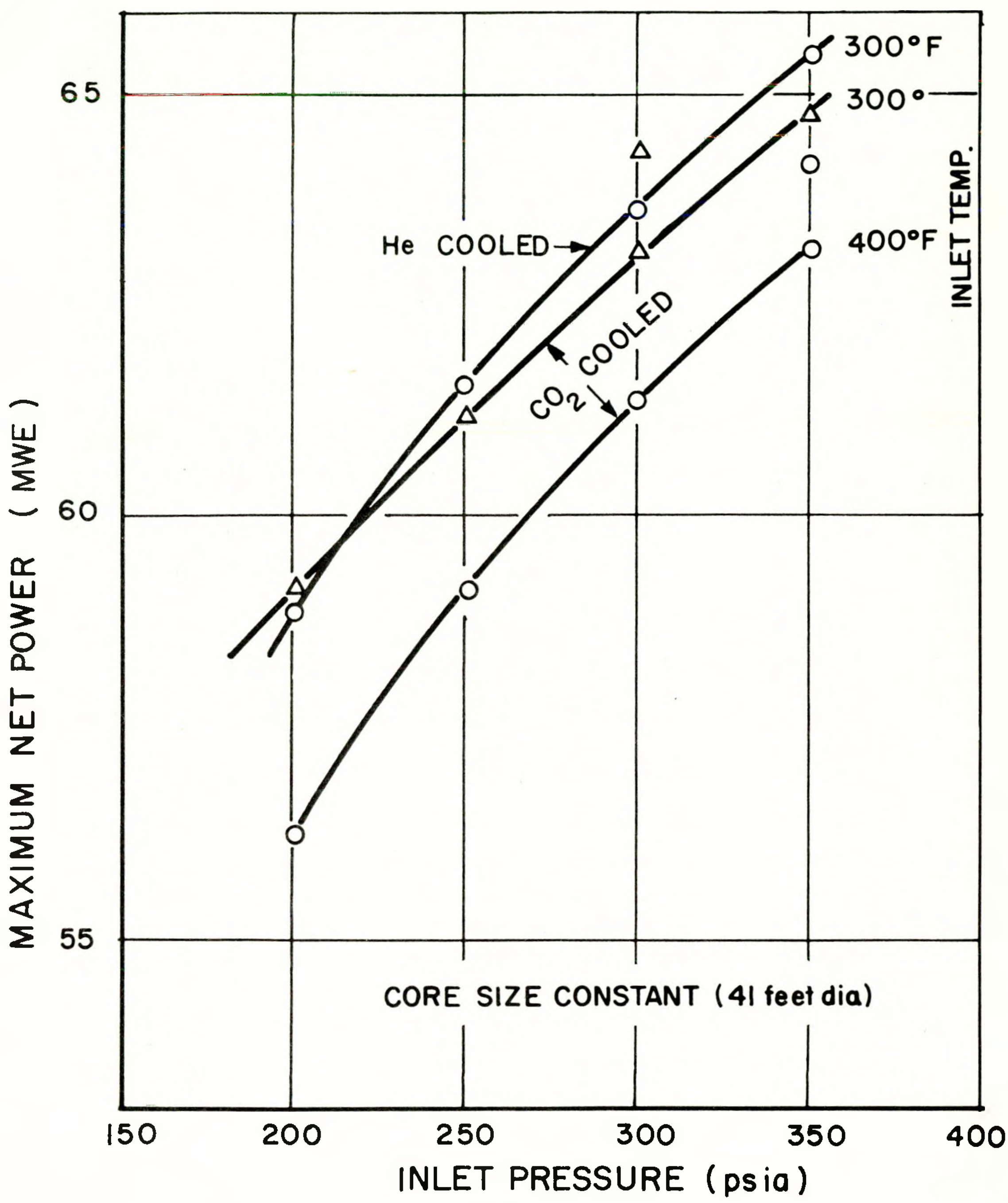


Fig. 9. Variation of Maximum Net Power With Operating Pressure

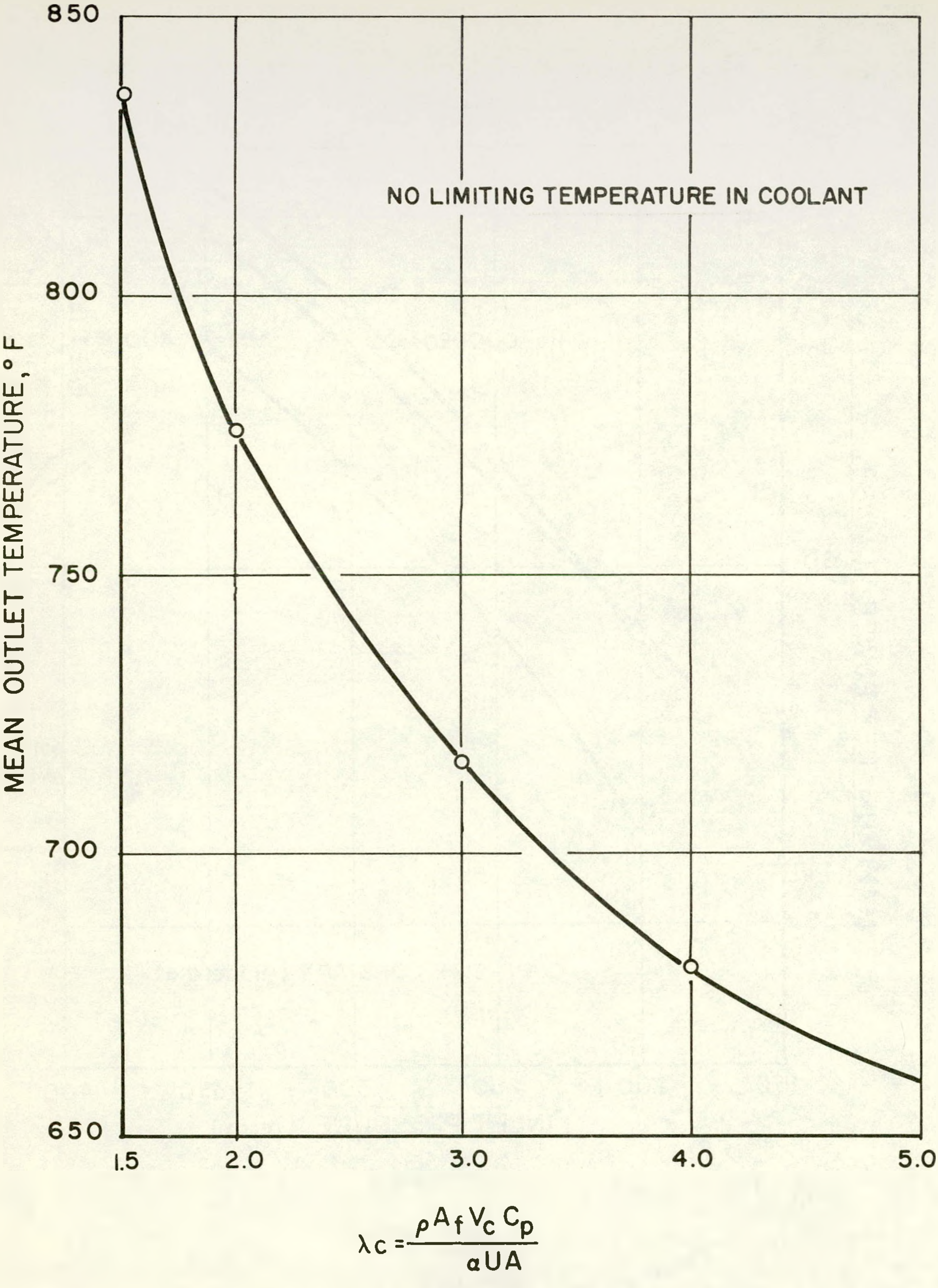


Fig. 10. Mean Outlet Temperature, No Coolant Temperature Limit

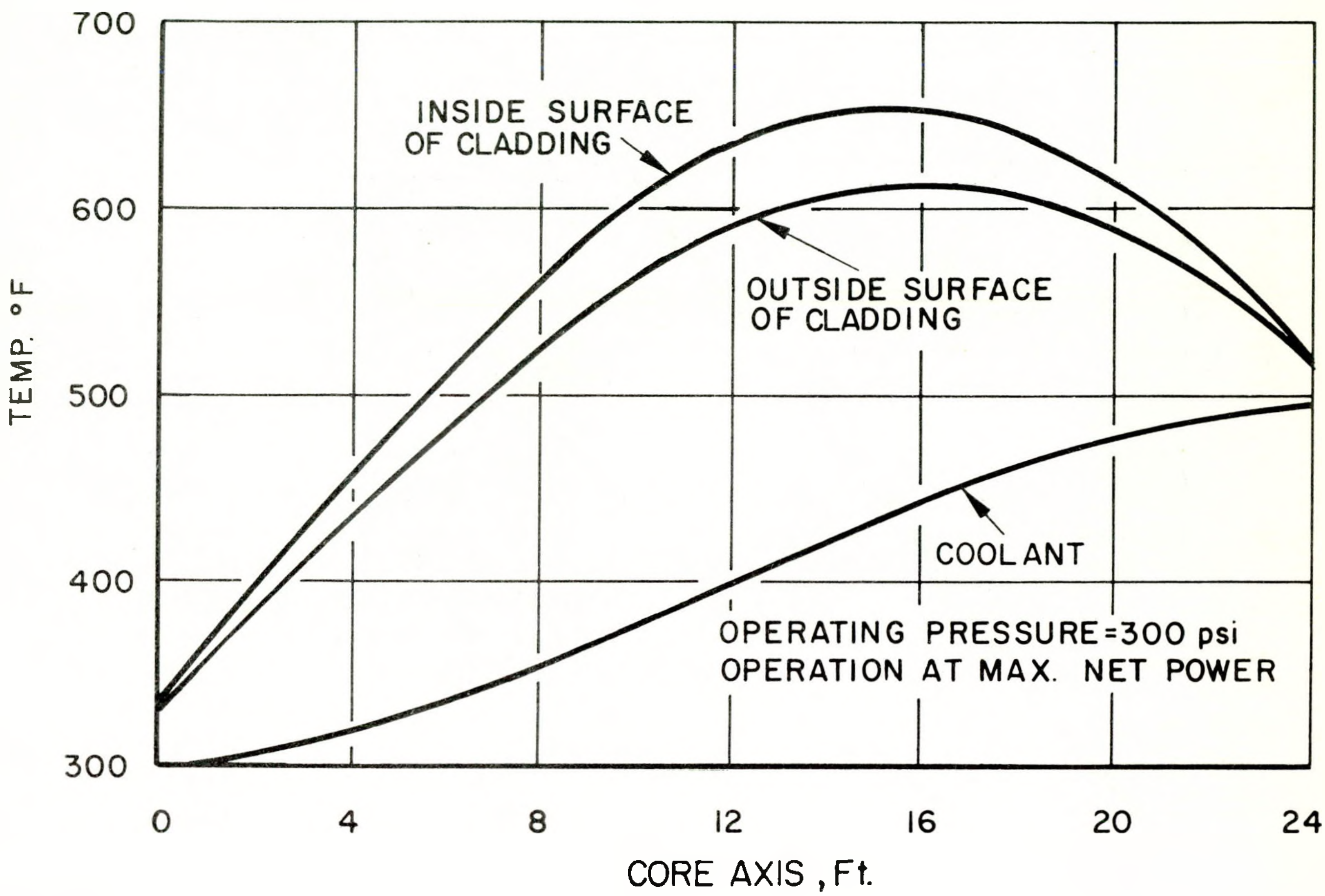


Fig. 11. Axial Temperature Variation, Center Channel

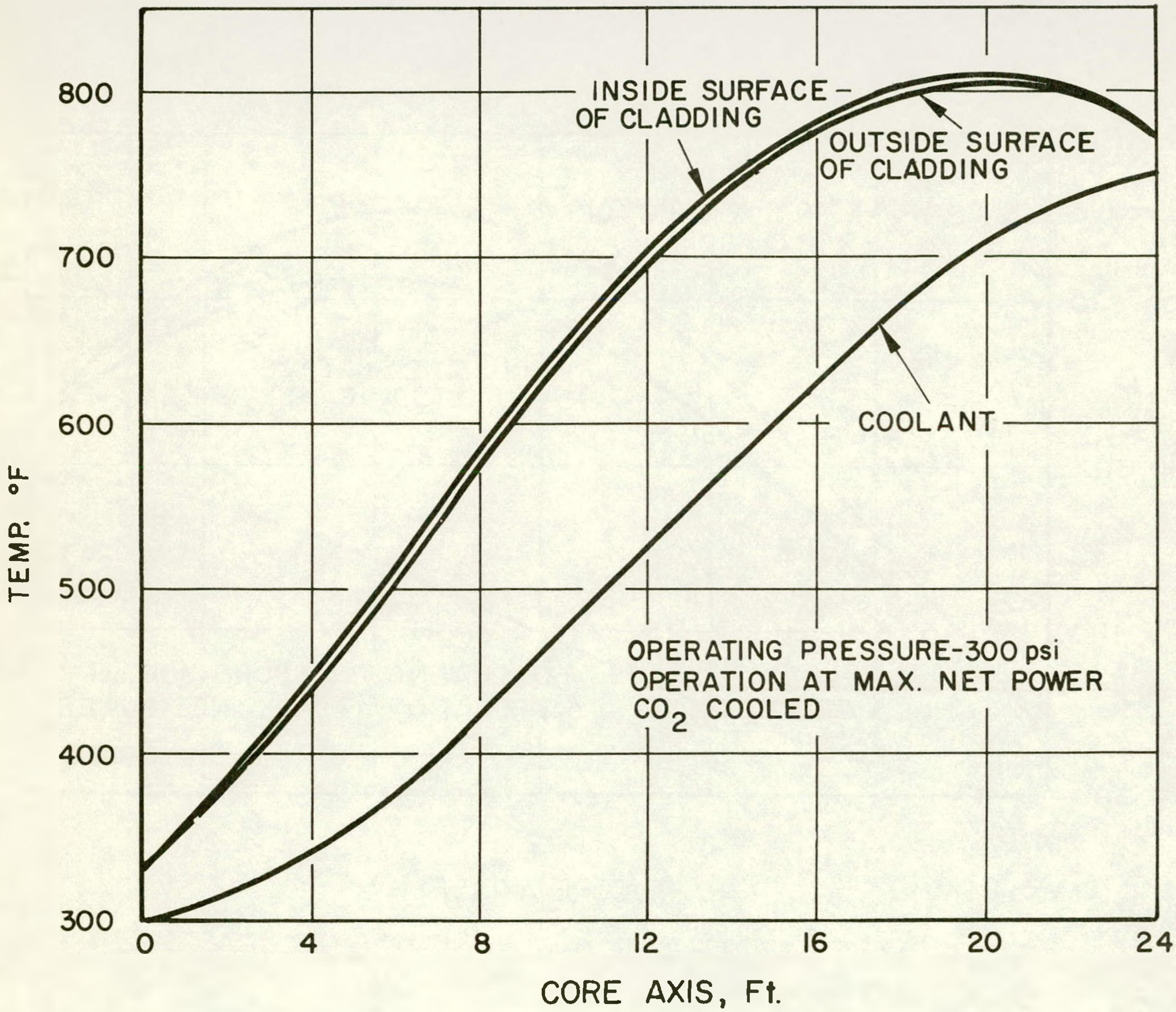
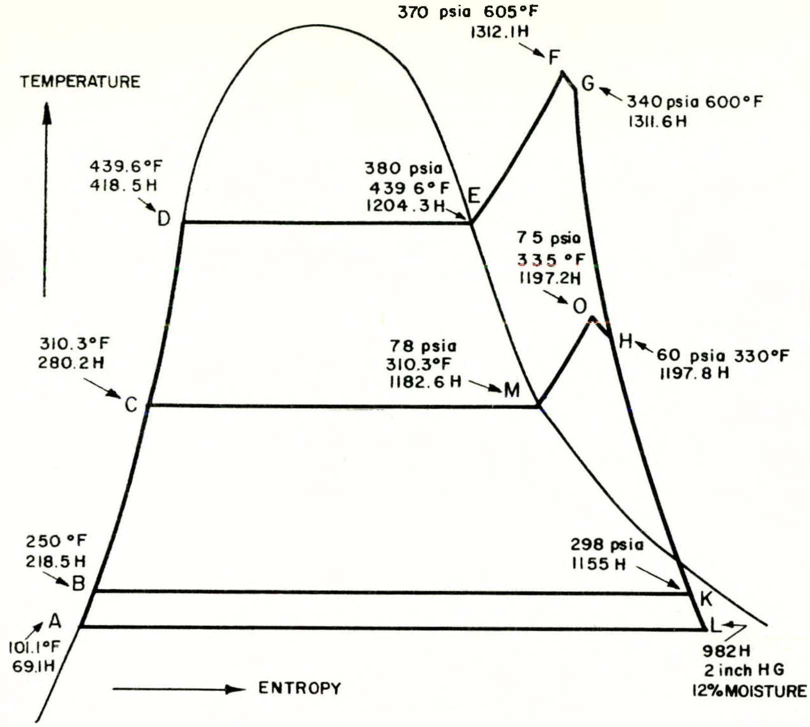


Fig. 12. Axial Temperature Variation, Outer Channel



TEMPERATURE ENTROPY DIAGRAM OF DUAL STEAM CYCLE

LEGEND

A-B = Preheating of Total Feedwater from Condenser in Deaerator

K-B = Extraction Steam from Steam Turbine to Preheat Feedwater in Deaerator

LOW PRESSURE CYCLE		HIGH PRESSURE CYCLE	
B-C =	Preheating of Low Pressure Cycle water in Economizer	B-D =	Preheating of High Pressure Cycle Water in Economizer
C-M =	Evaporating in Low Pressure Boiler	D-E =	Evaporating of High Pressure Water in Boiler
M-O =	Superheating in Low Pressure Superheater	E-F =	Superheating of Steam in Superheater
O-H =	Steam Pressure & Temperature Drops in Piping From Superheater Outlet to Turbine Inlet	F-G =	Steam Pressure & Temperature Drops in Piping From Superheater Outlet to Turbine Admission Point
H-L =	Expansion Line of Low Pressure Steam in Turbine	G-L =	Expansion of High Pressure Steam in Turbine
L-A =	Condensing of Turbine Exhaust Steam in Condenser	L-A =	Condensing of High Pressure Steam in Condenser

Fig. 13. Temperature-Entropy Diagram

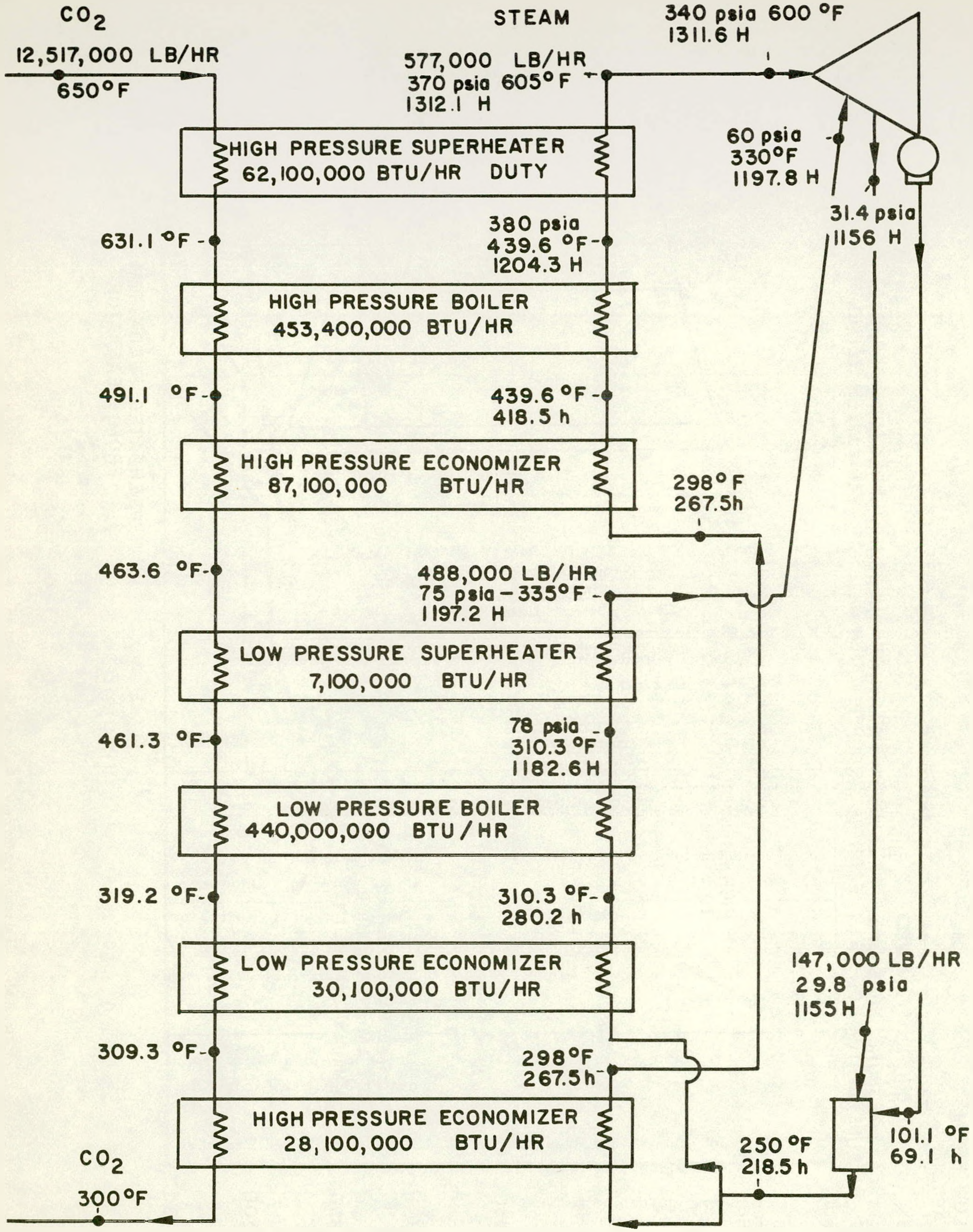


Fig. 14. Detailed Steam Generator Heat Balance

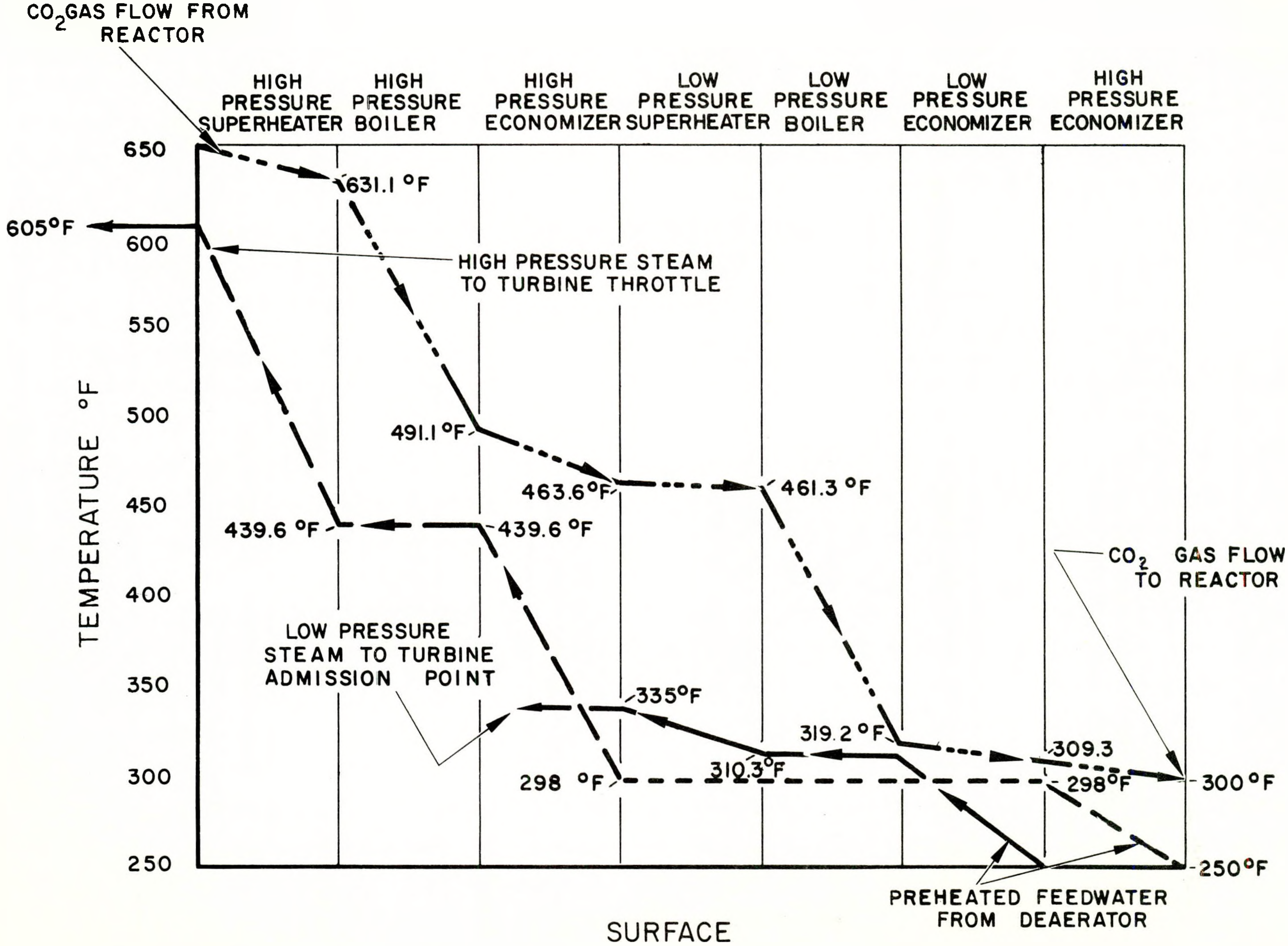


Fig. 15. Temperature-Surface Diagram, Steam Generator

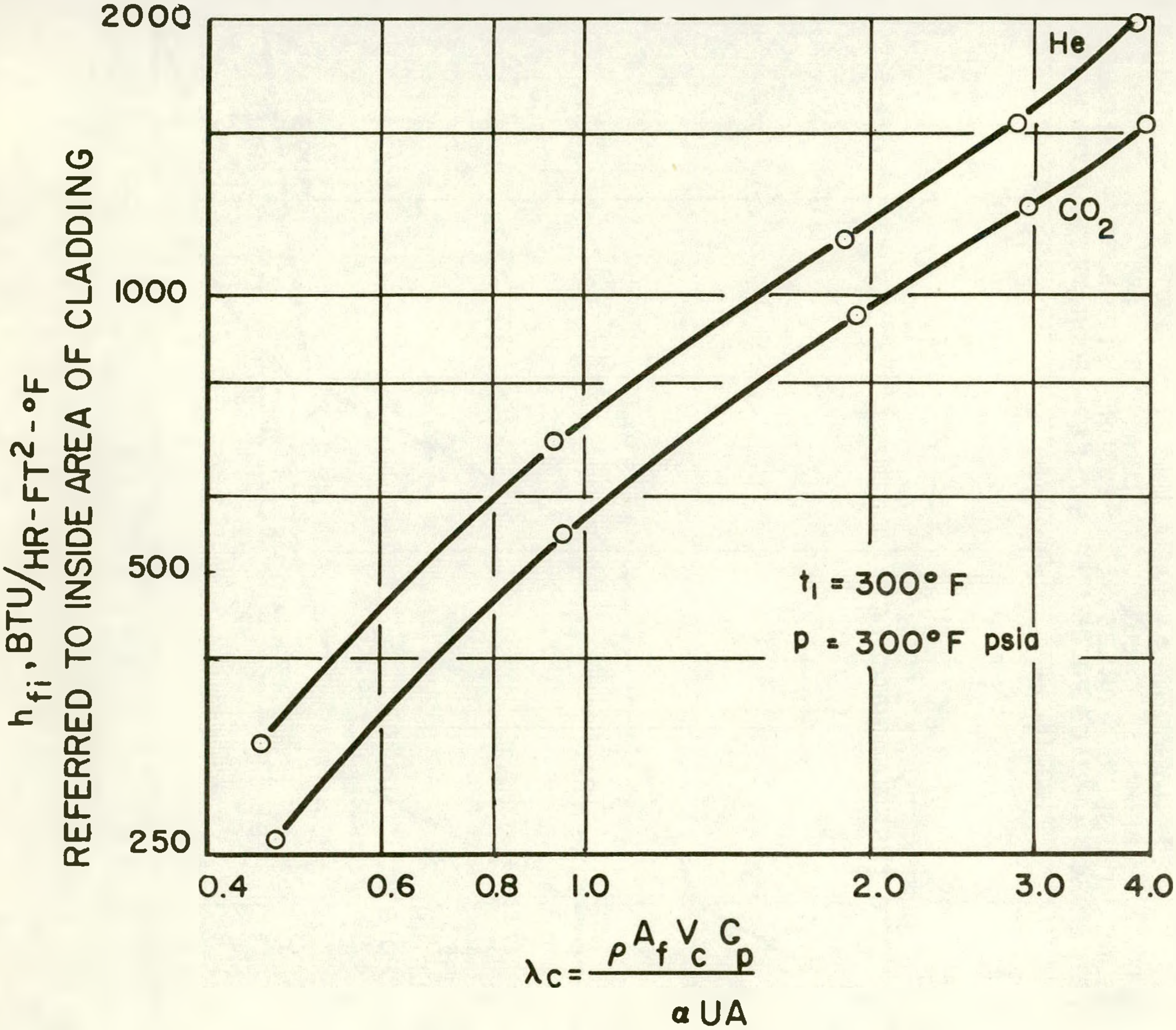


Fig. 16. Film Coefficients

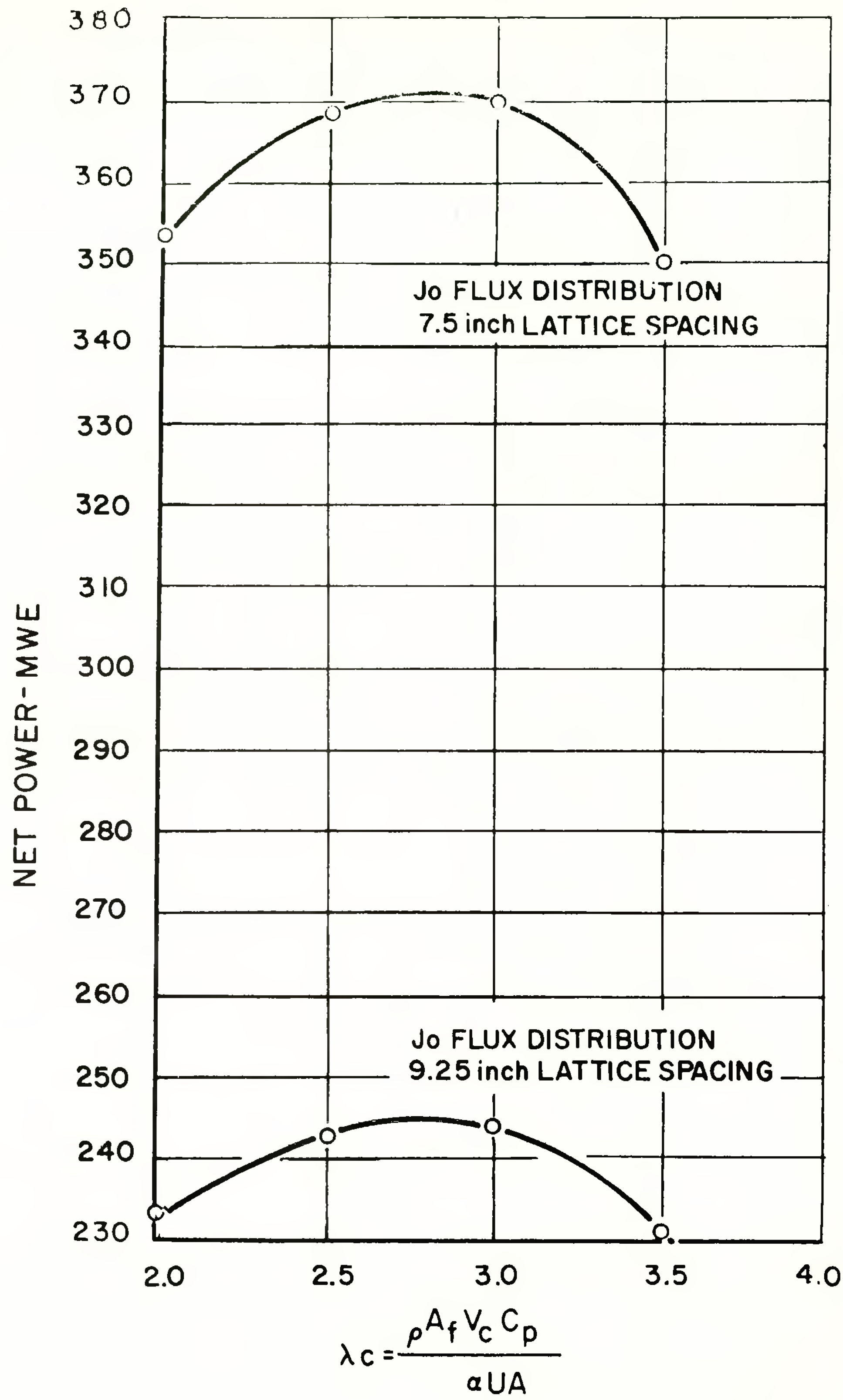


Fig. 17. Net Electric Power, 95 Ft. Vessel

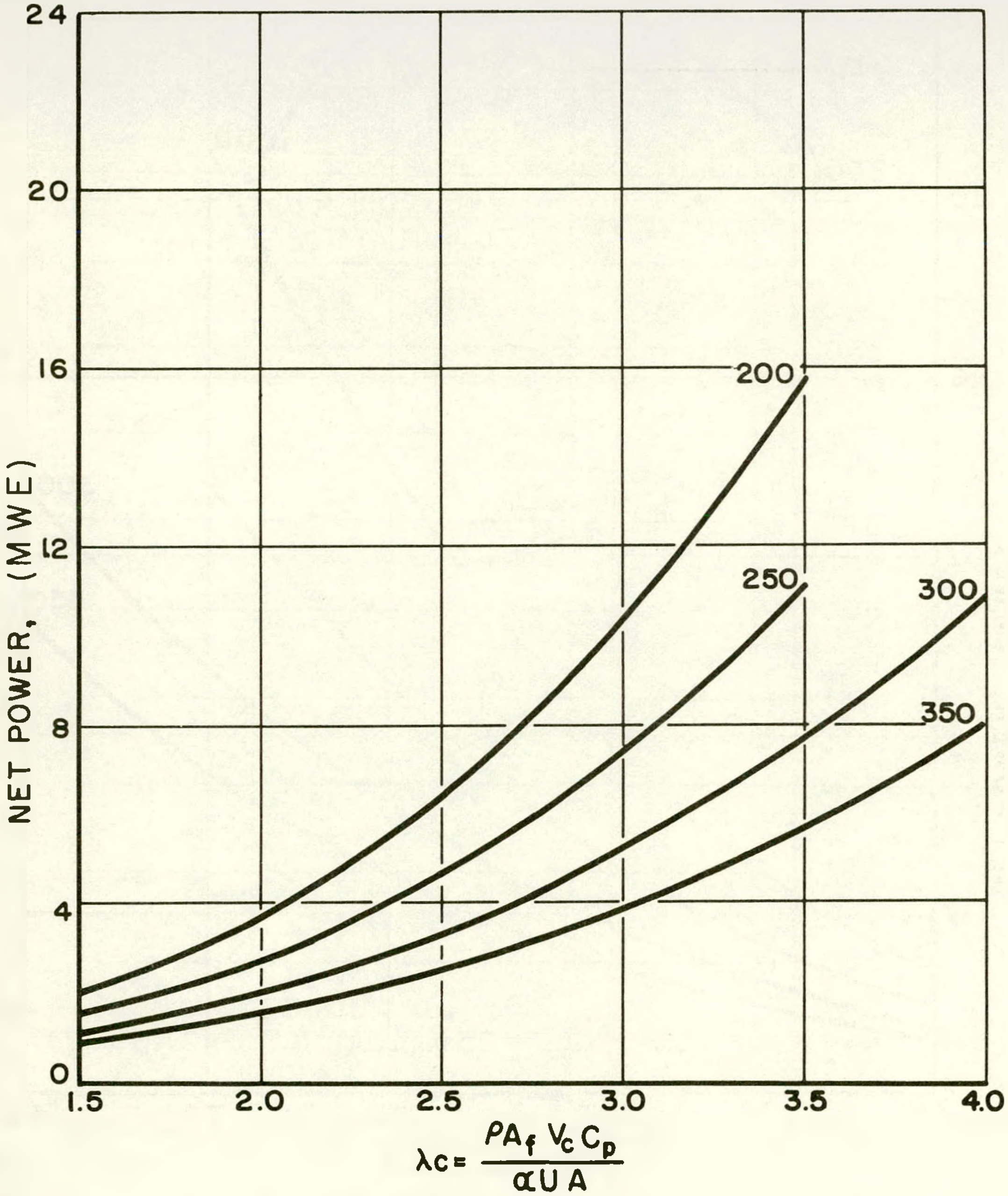


Fig. 18. Heat Transfer Number (λ_c) vs Net Power (Inlet Temp. 300° F)

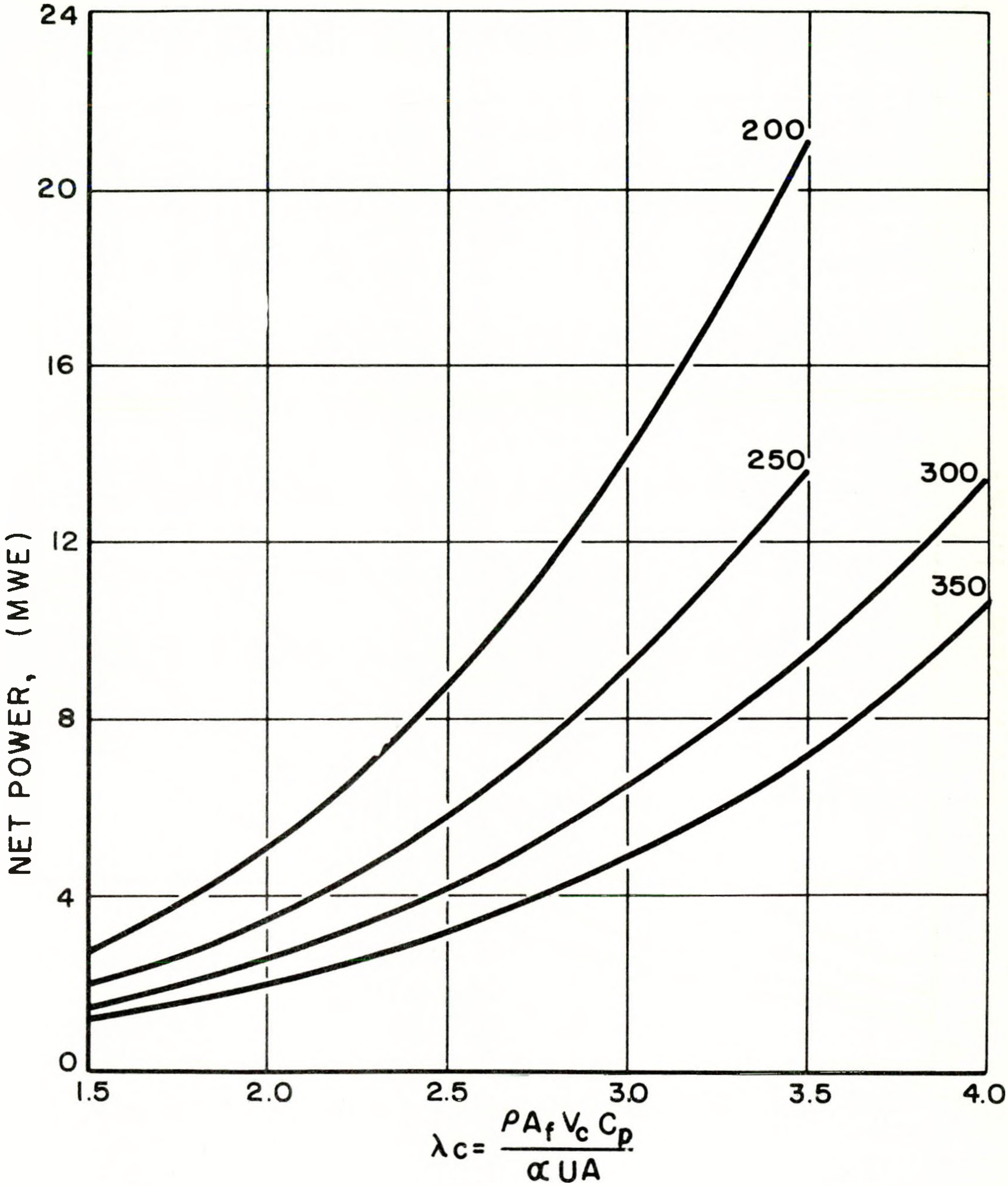
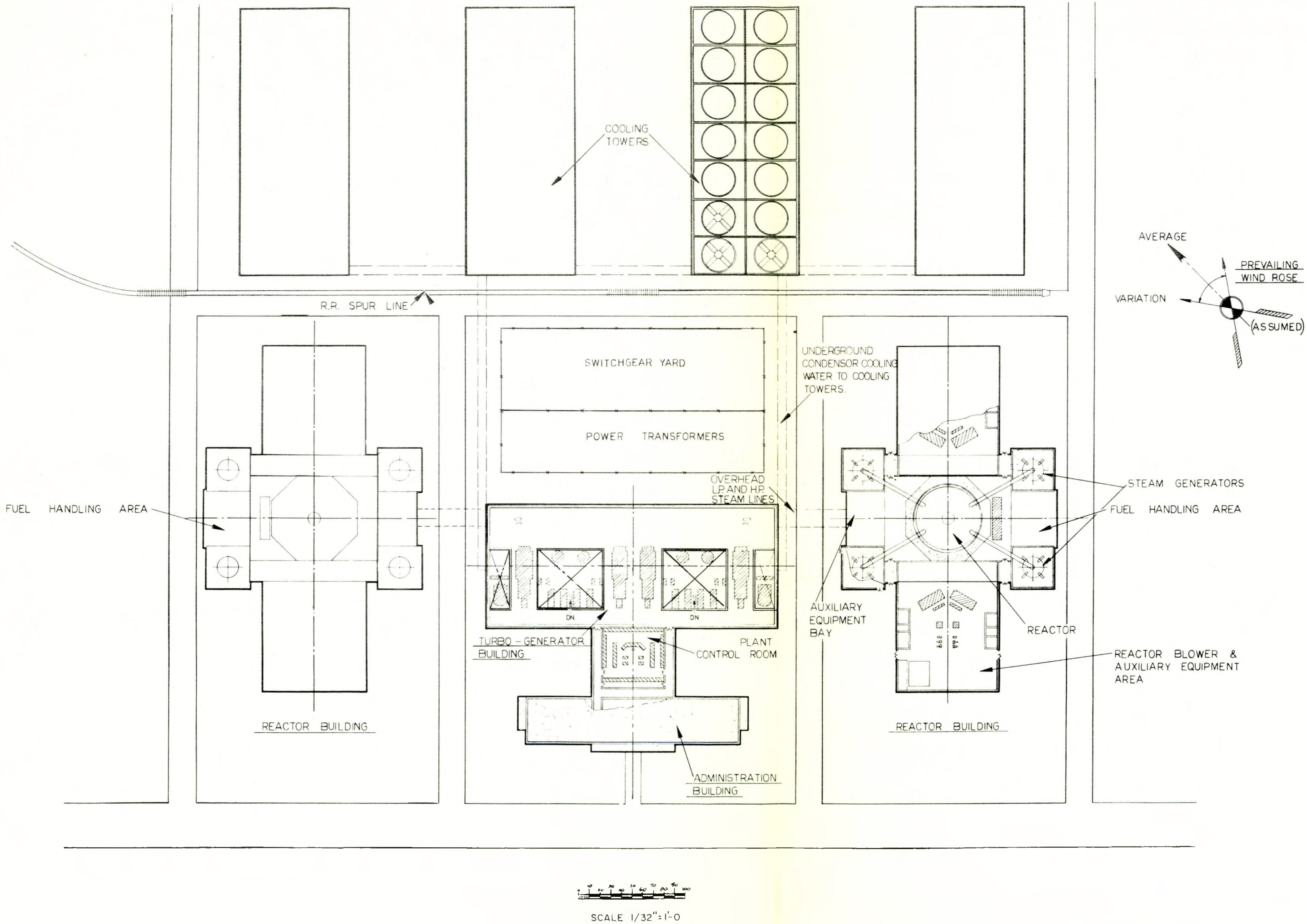
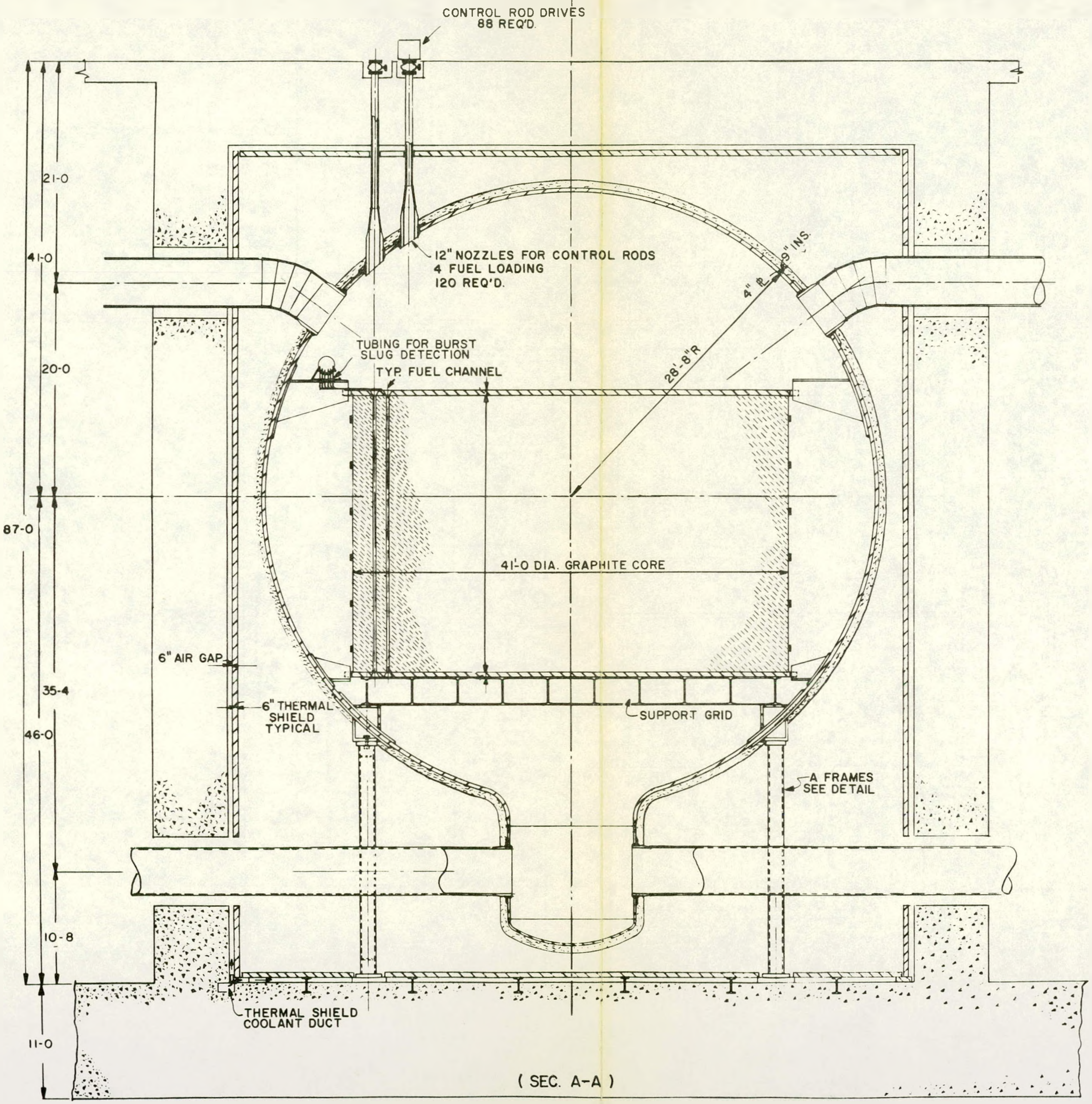
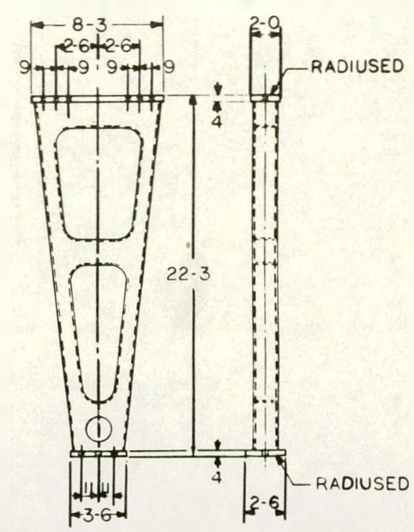


Fig. 19. Heat Transfer Number (λ_c) vs Net Power (Inlet Temp. 400° F)



Dwg. 1. Facilities Layout (AE70-981001)

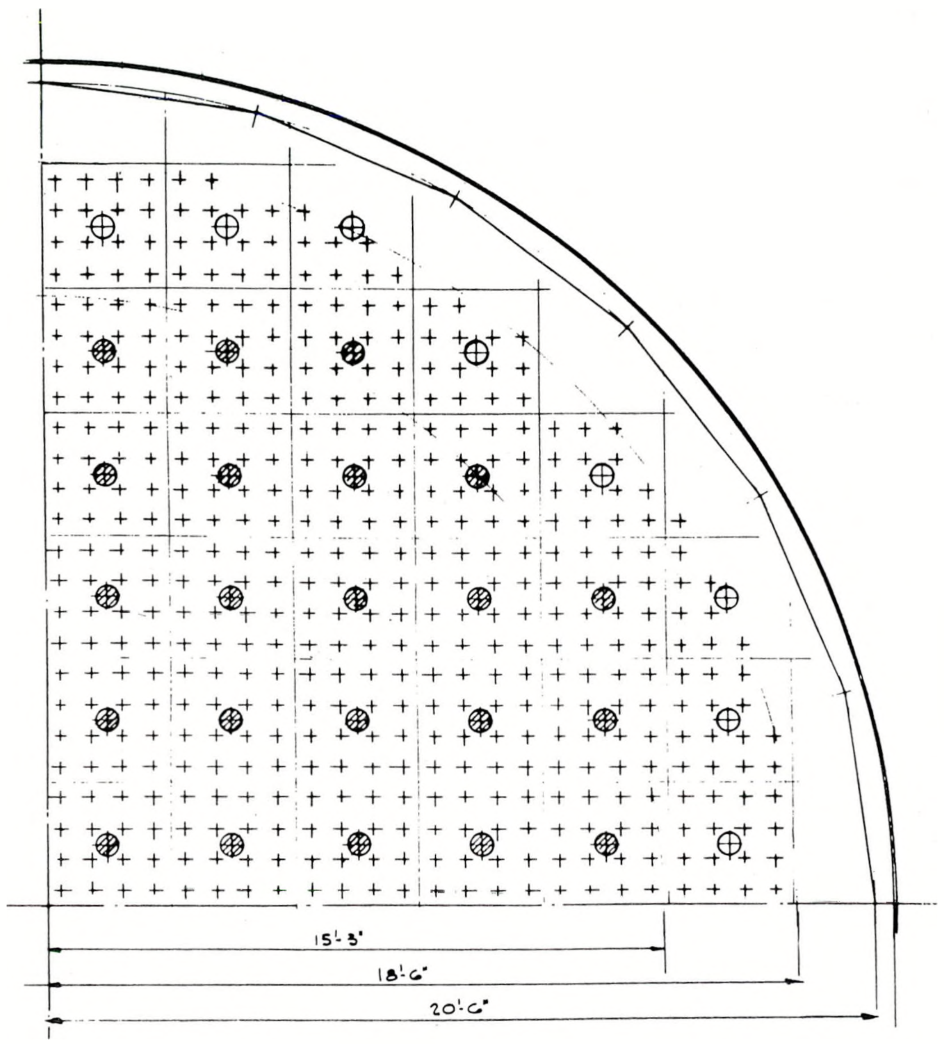




DETAIL OF A FRAME (12 REQ'D)

Dwg. 2. Reactor Section
(AE70-971002)

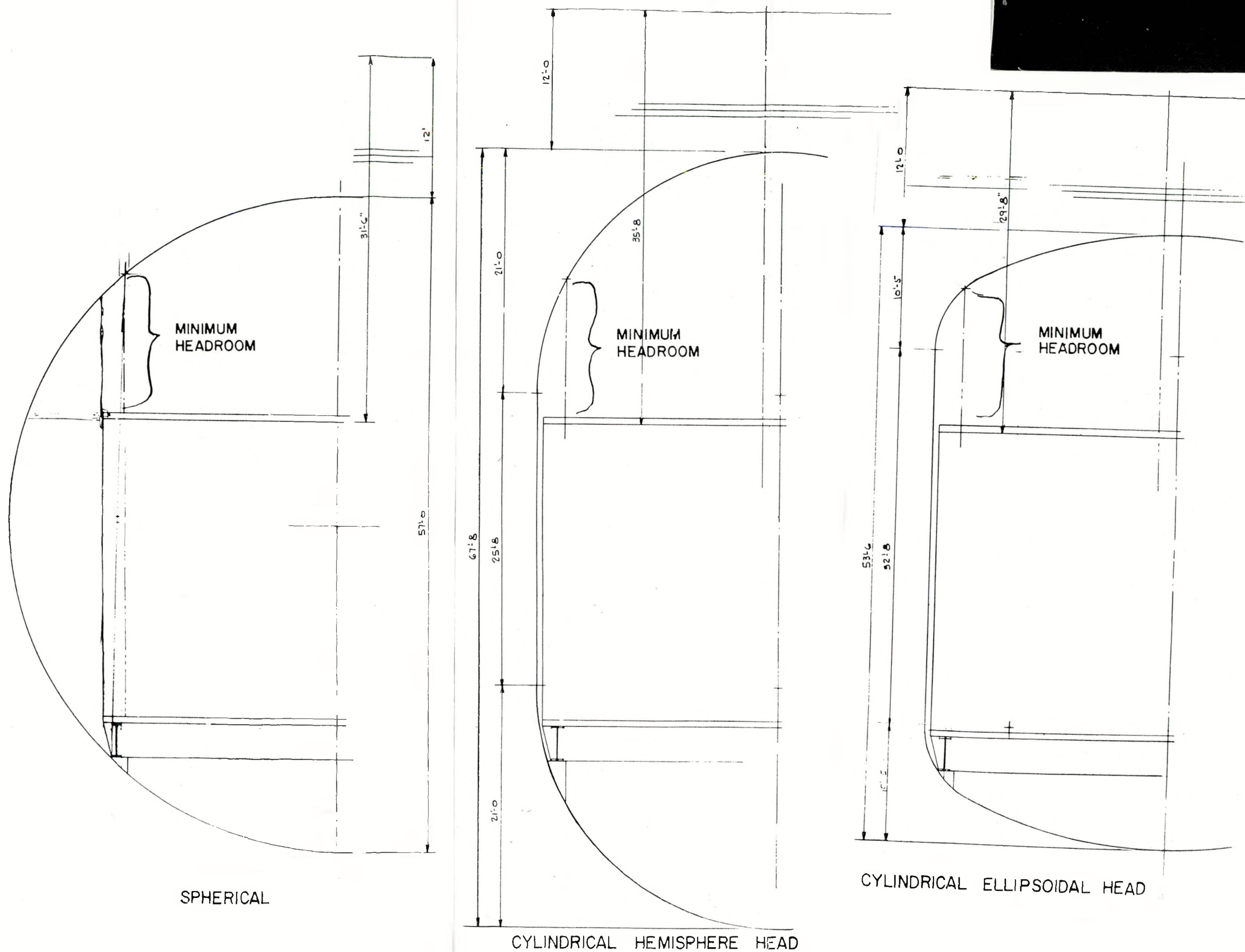
[redacted]



PLAN

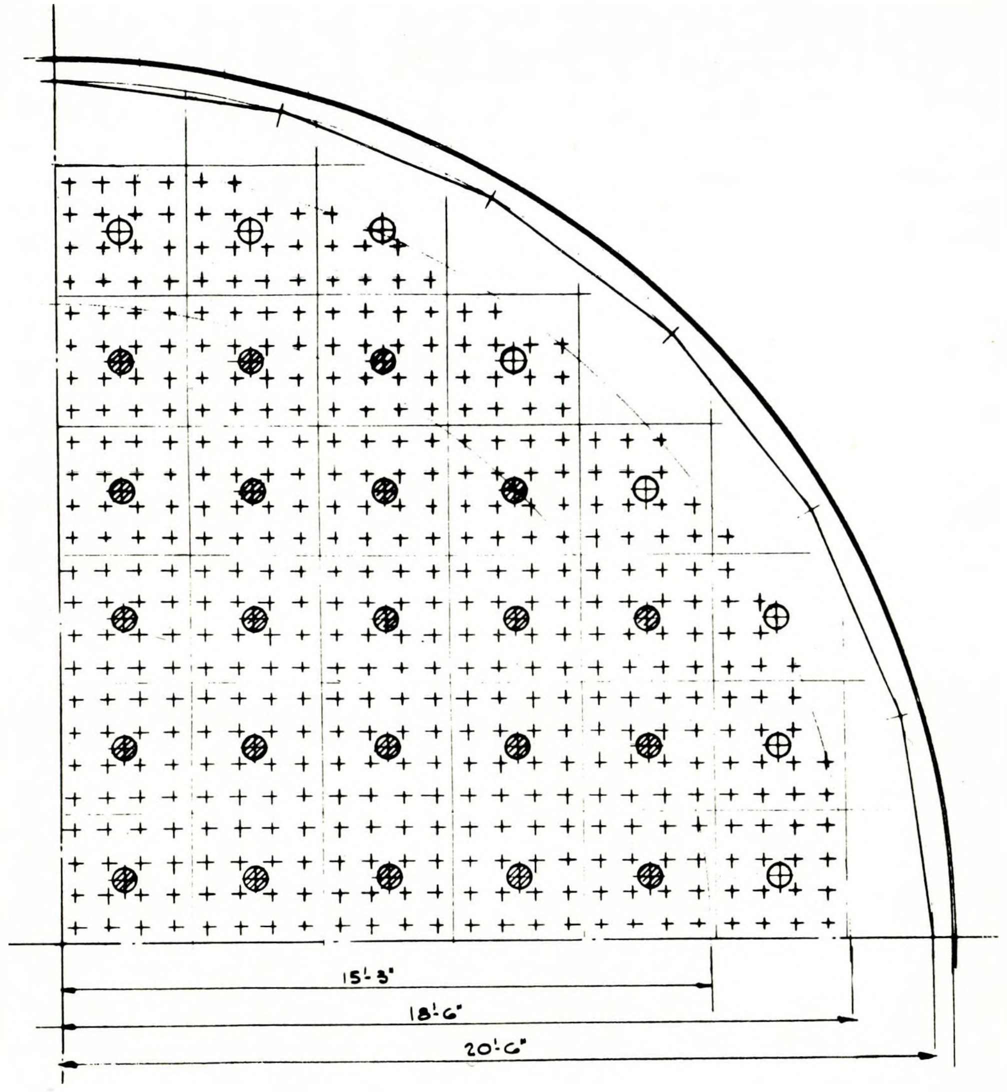
CORE LAYOUT

- Control & Safety - 37"cc. — 22 x 4 · 88
- Additional Fuel Loading Holes — 18 x 4 · 32
- Fuel Cells · 9¼" c.c. — 456 x 4 · 1824
- Core Size · 41'-0 Dia. x 27'-0 Depth



VESSEL TYPES, ELEVATIONS

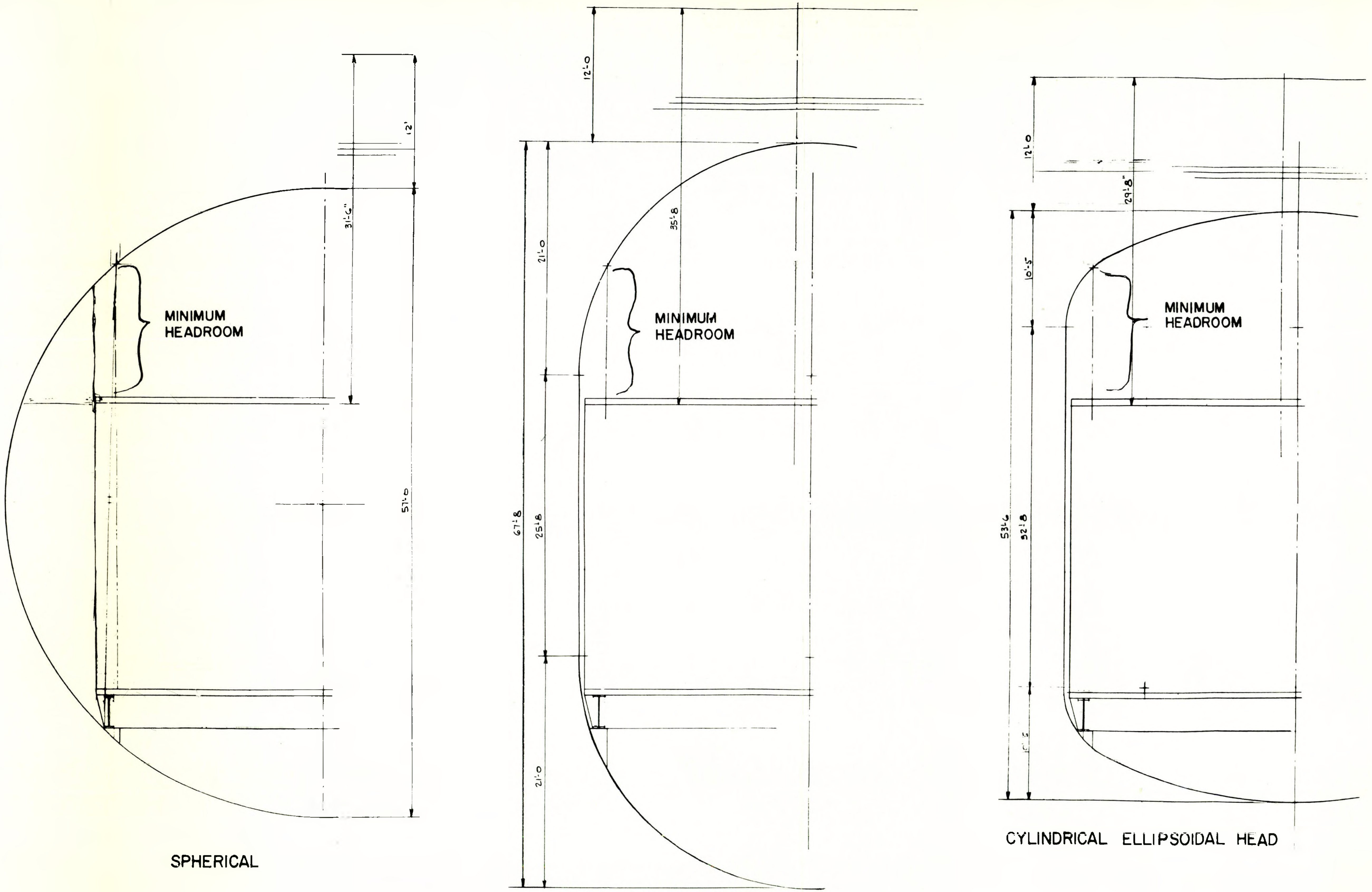
Dwg. 3. Core Layout (Sketch No. 1



PLAN

CORE LAYOUT

- Control & Safety - 37" c.c. — 22 × 4 = 88
- Additional Fuel Loading Holes — 8 × 4 = 32
- Fuel Cells - 9¼" c.c. — 456 × 4 = 1824
- Core Size - 41'-0 Dia. × 27'-0 Depth



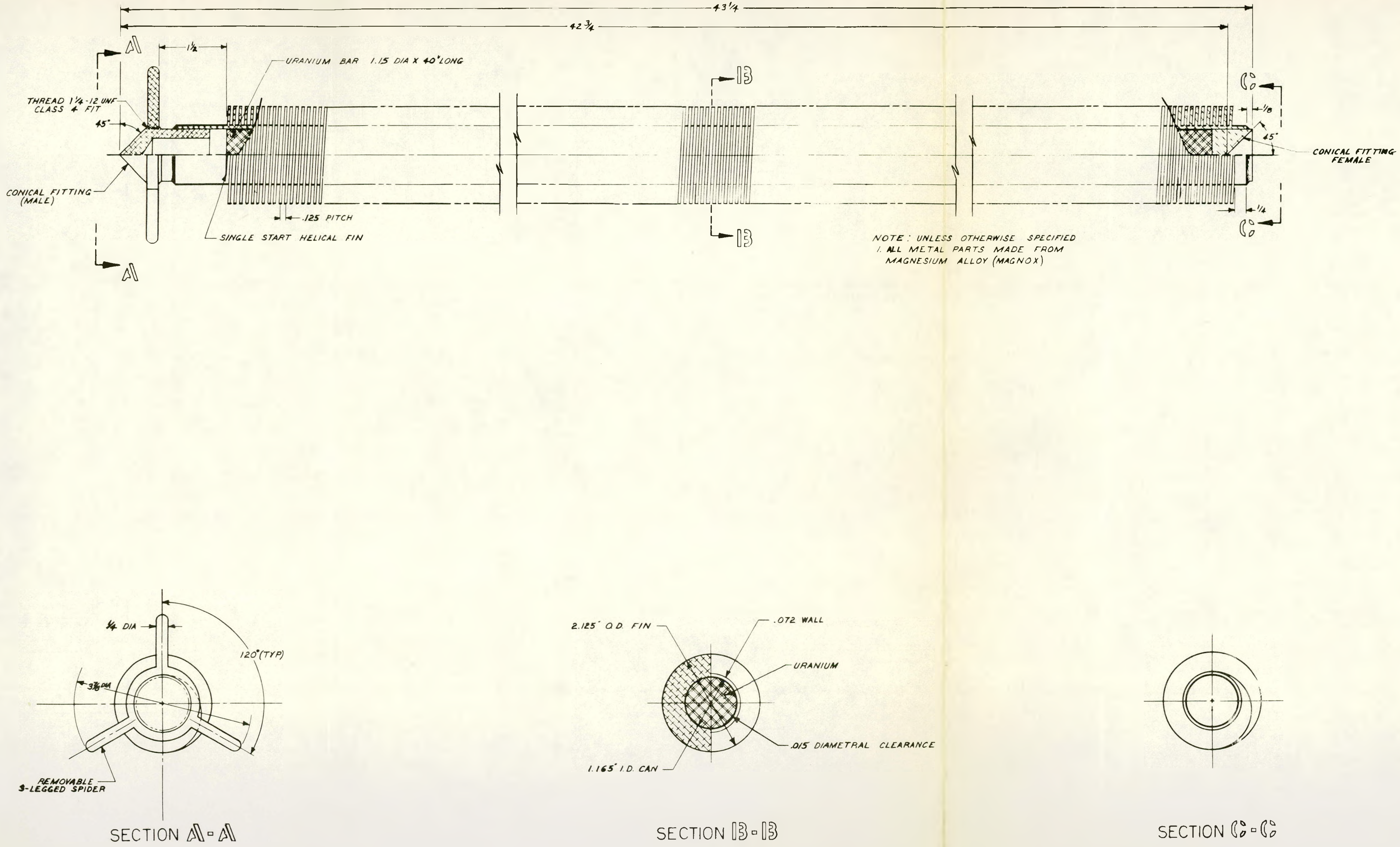
SPHERICAL

CYLINDRICAL HEMISPHERE HEAD

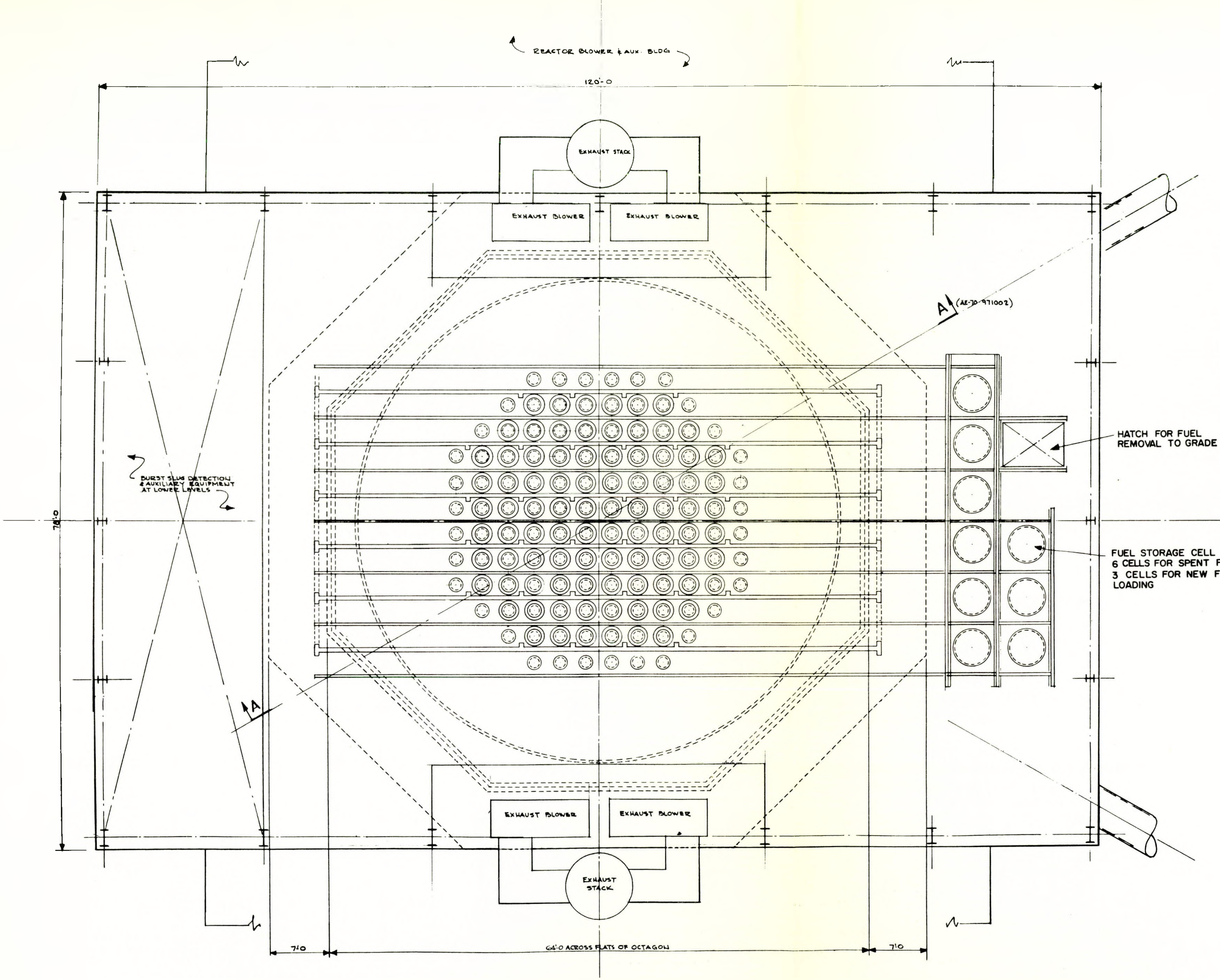
CYLINDRICAL ELLIPSOIDAL HEAD

VESSEL TYPES, ELEVATIONS

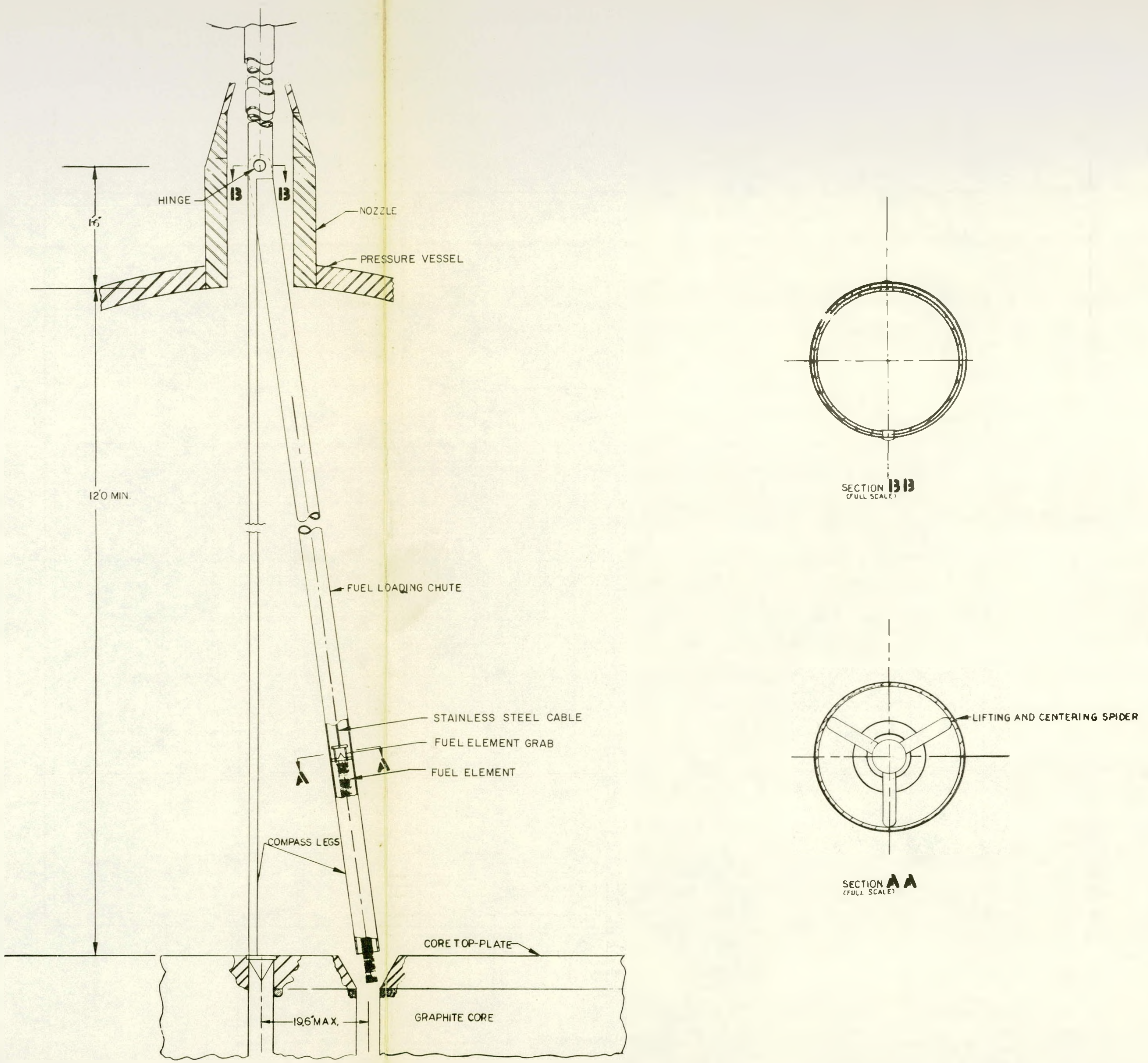
Dwg. 3. Core Layout (Sketch No. 1)



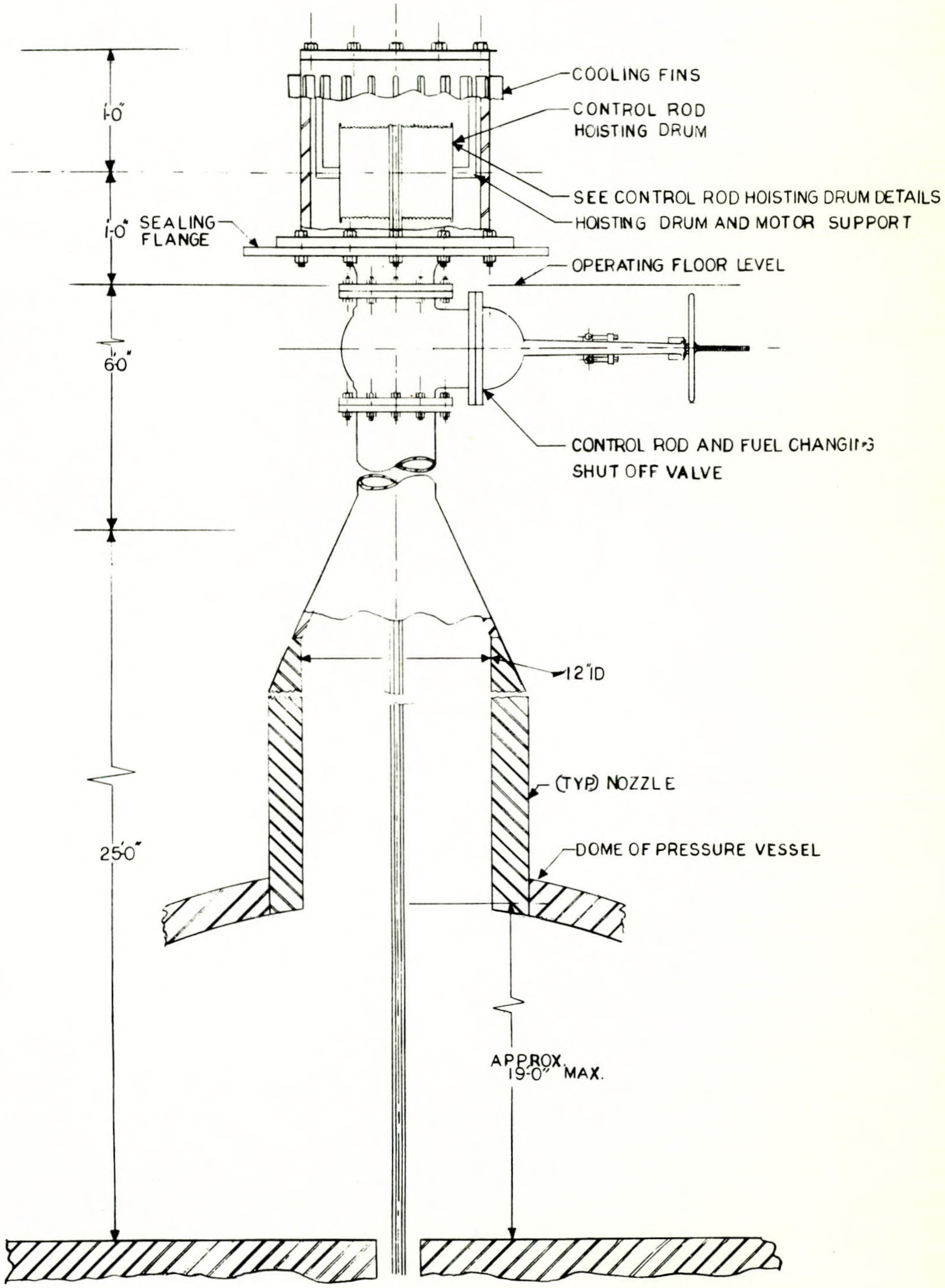
Dwg. 4. Fuel Element
(AE70-971801)

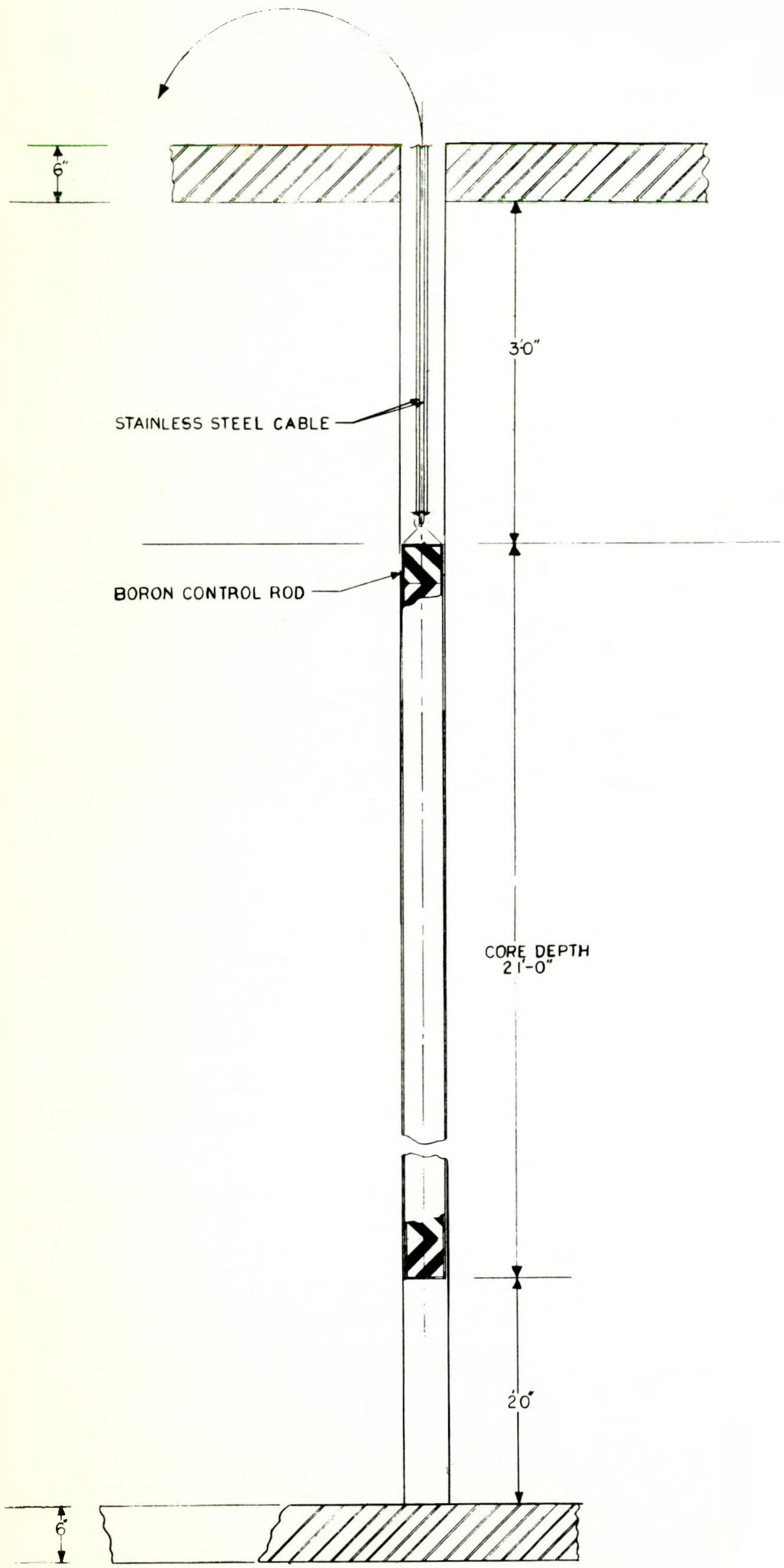


Dwg. 5. Loading Face
(AE70-971001)

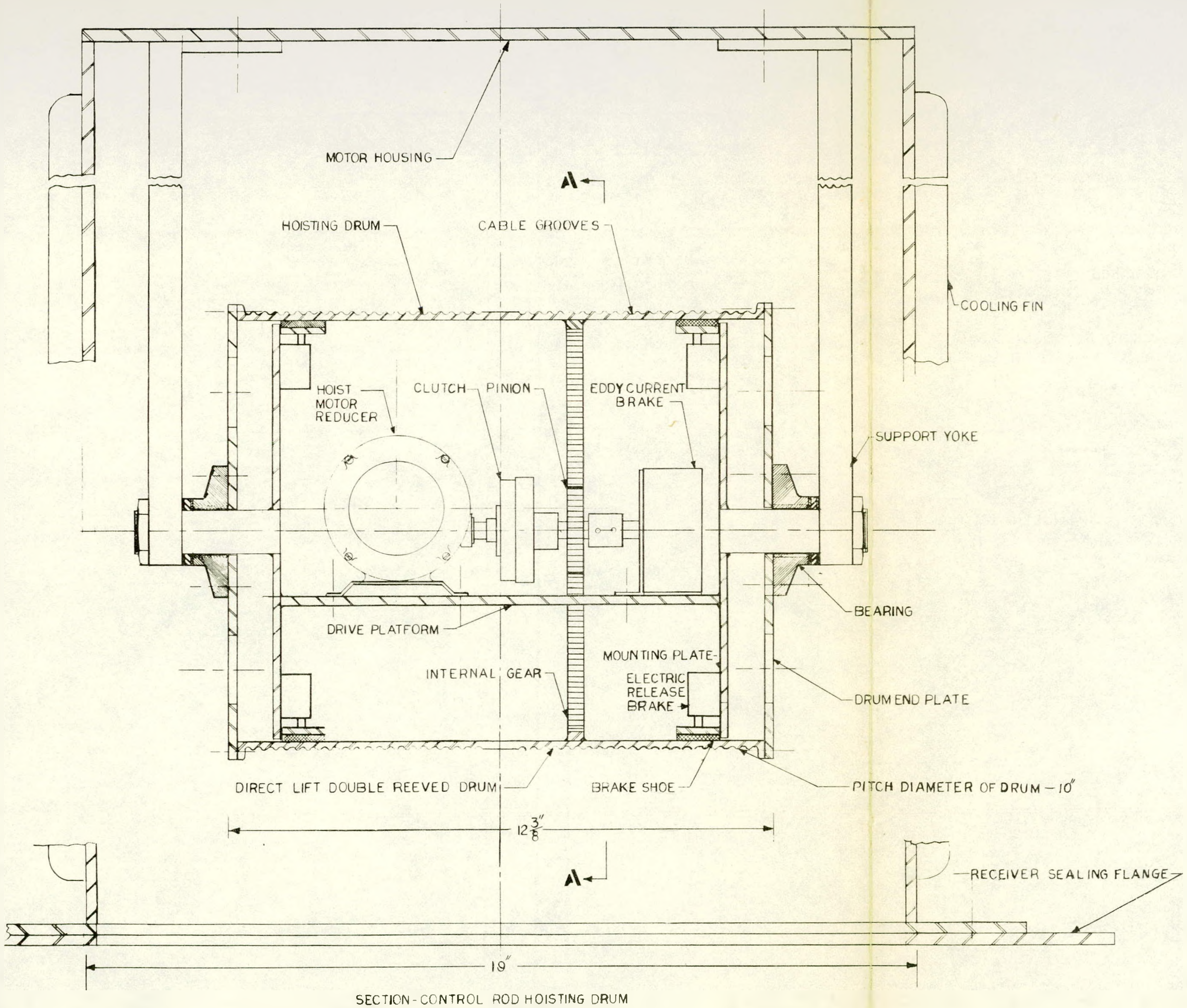


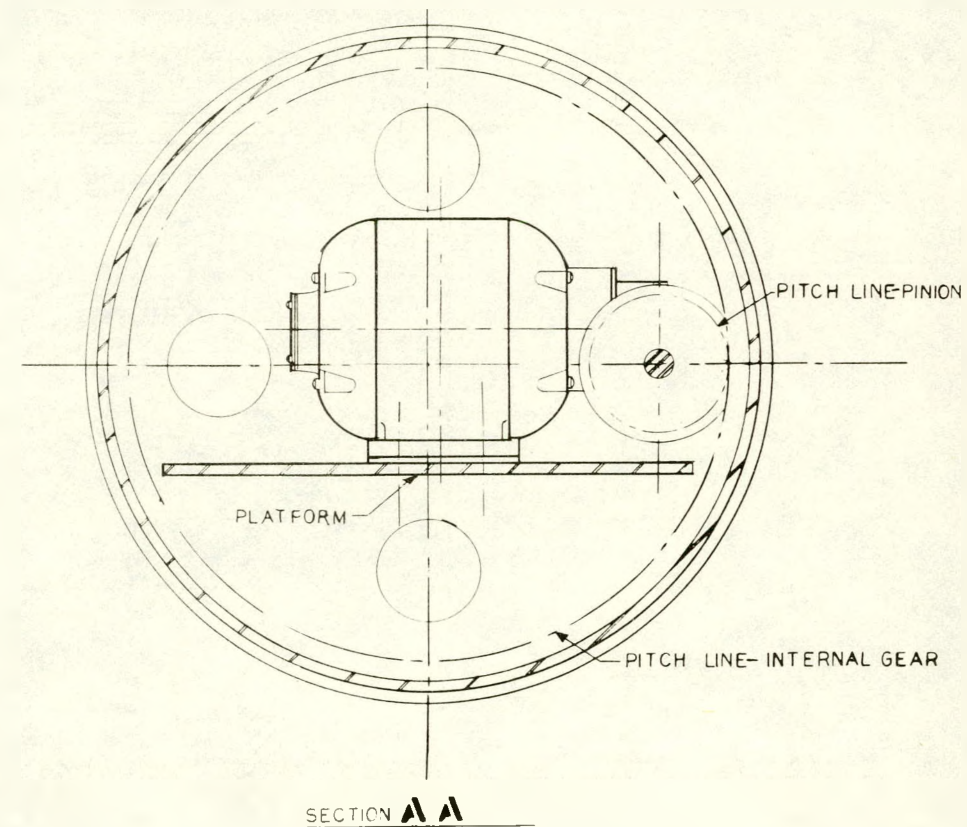
Dwg. 6. Fuel Changing Mechanism (AE70-978601)



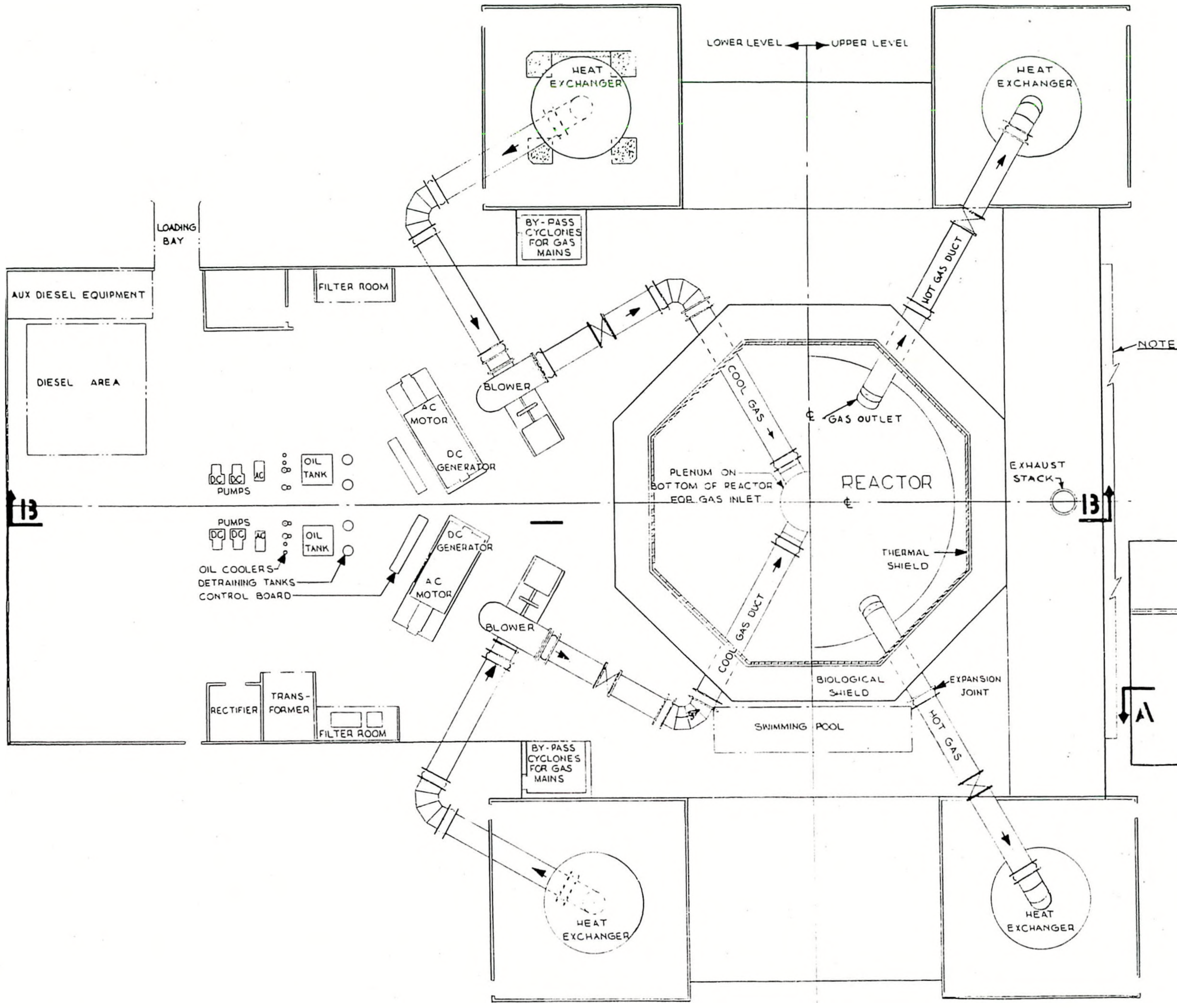


Dwg. 7. Control Rod & Drive
(AE70-971602)





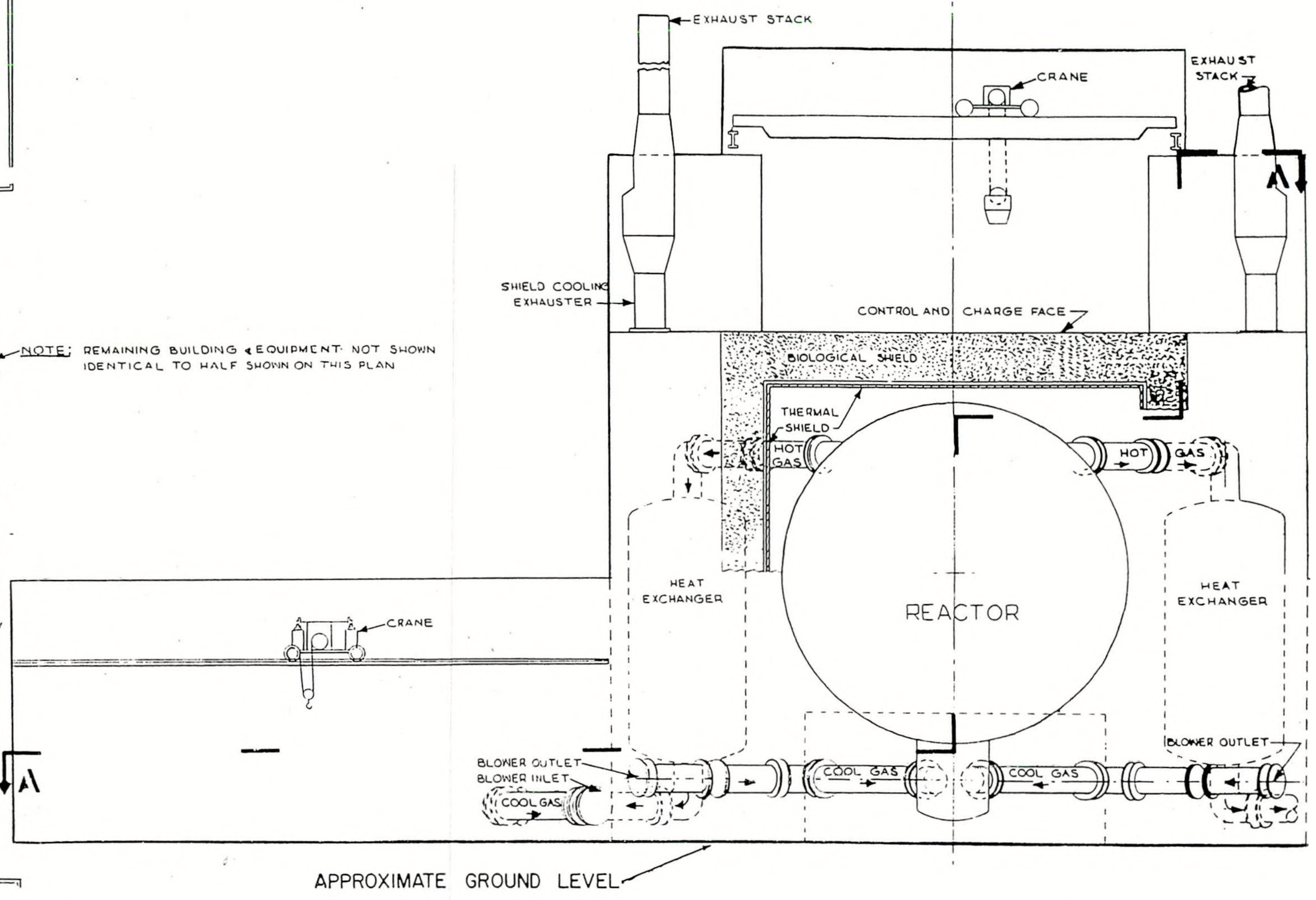
Dwg. 8. Control Rod Details
(AE70-971601)



PLAN VIEW A-A

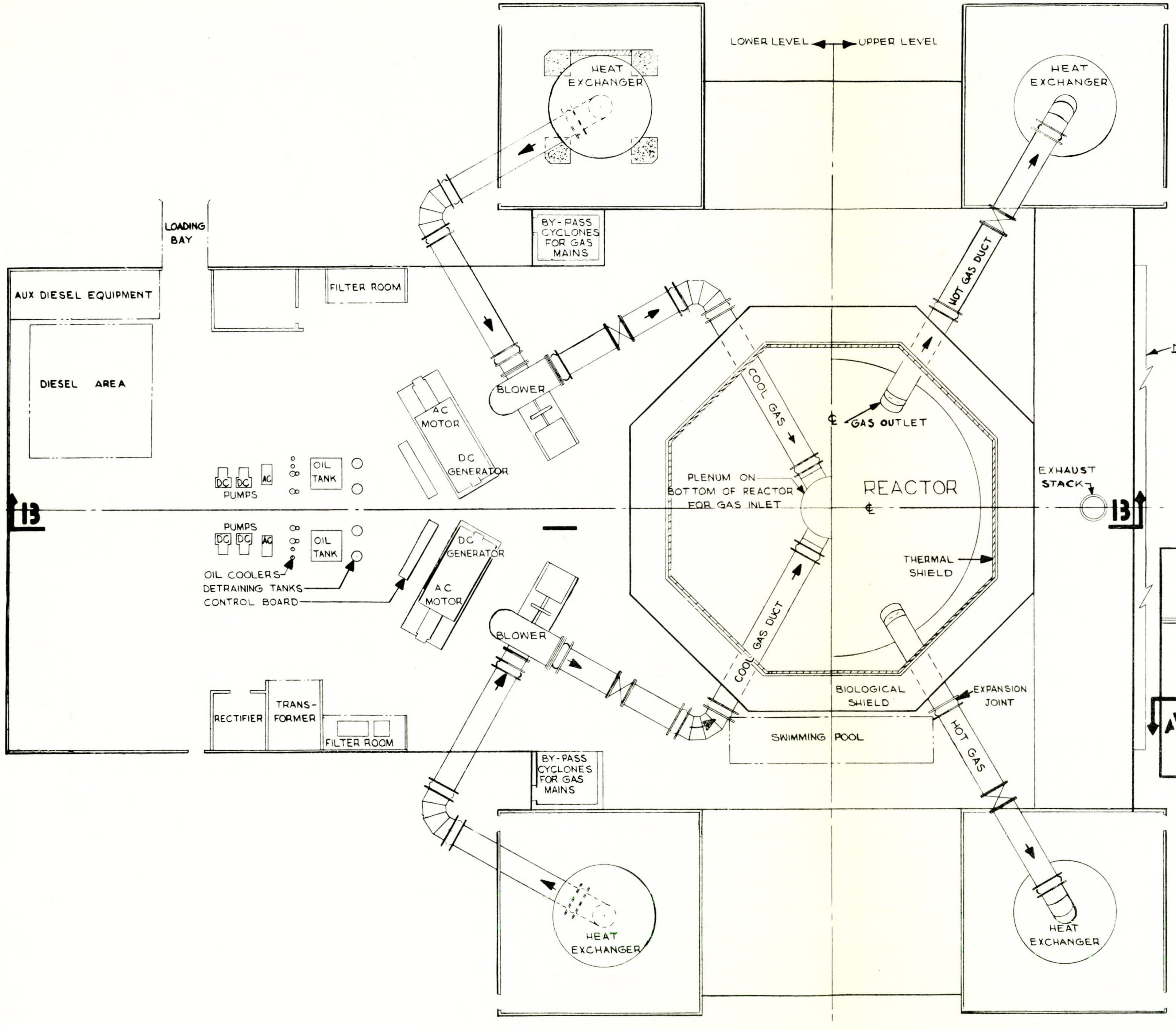
SHOWN WITH CEILINGS REMOVED

NOTE: REMAINING BUILDING & EQUIPMENT NOT SHOWN IDENTICAL TO HALF SHOWN ON THIS PLAN

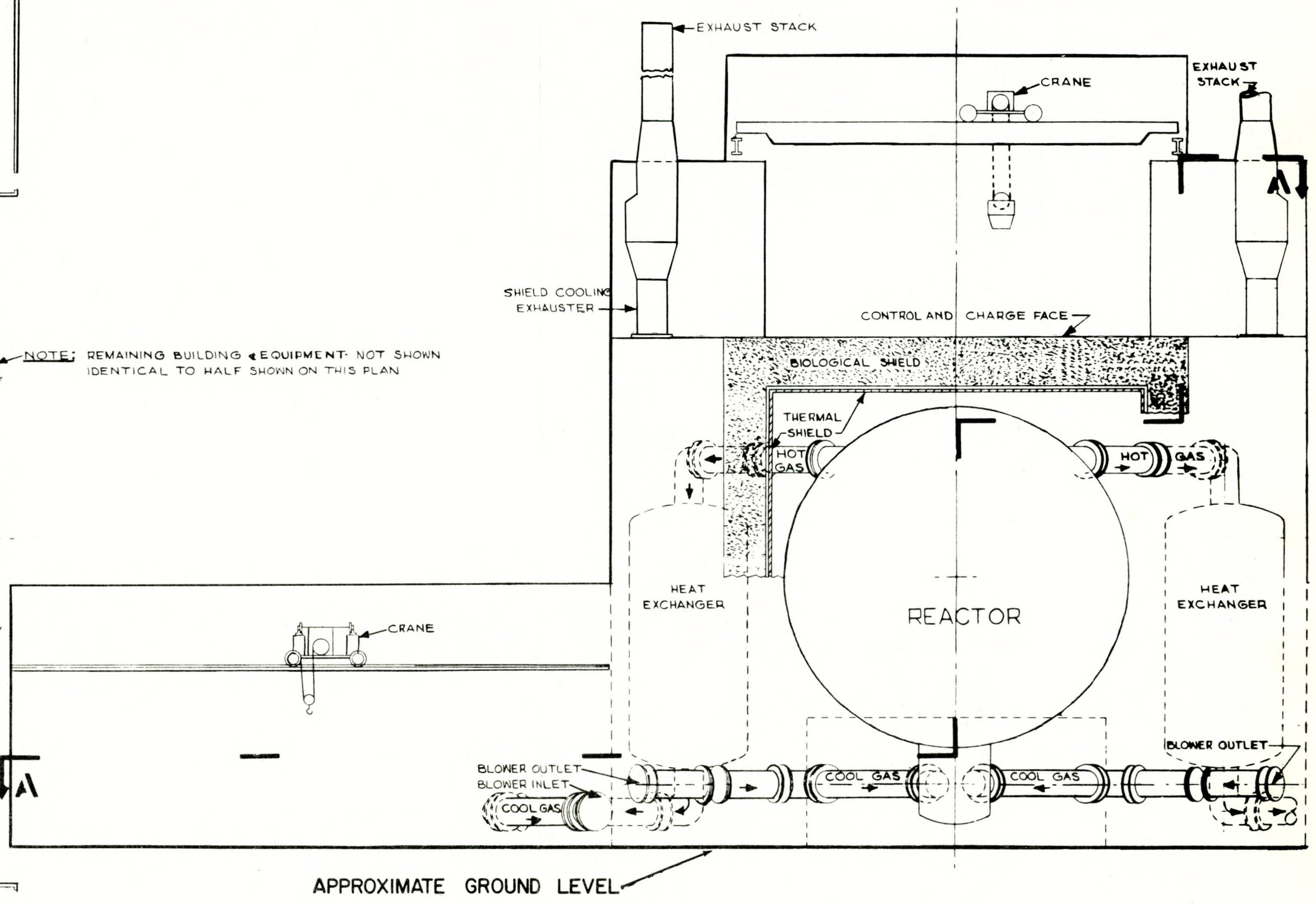


ELEVATION B-B

Dwg. 9. Heat Removal System - Piping (AE70-978001)

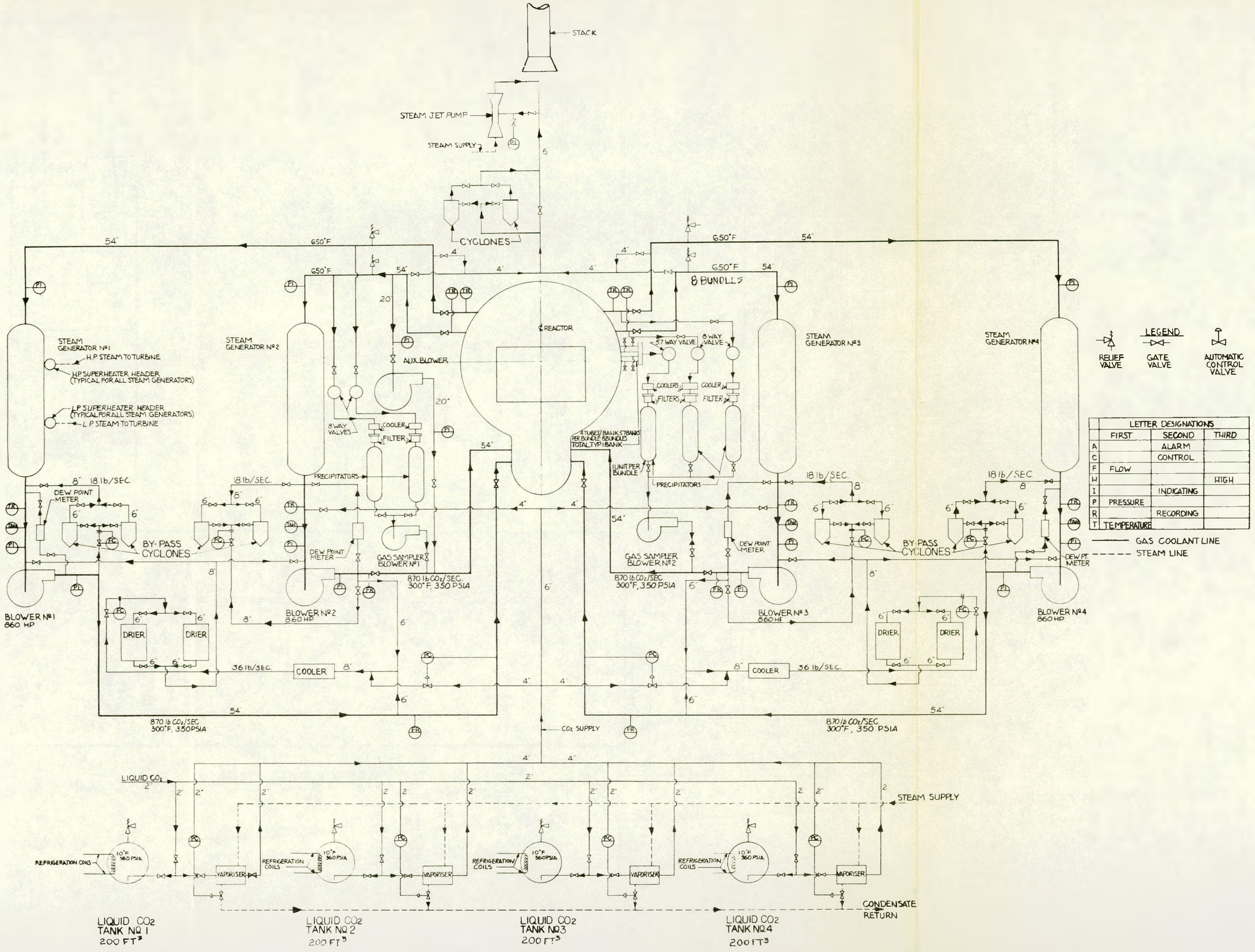


PLAN VIEW A-A
SHOWN WITH CEILINGS REMOVED

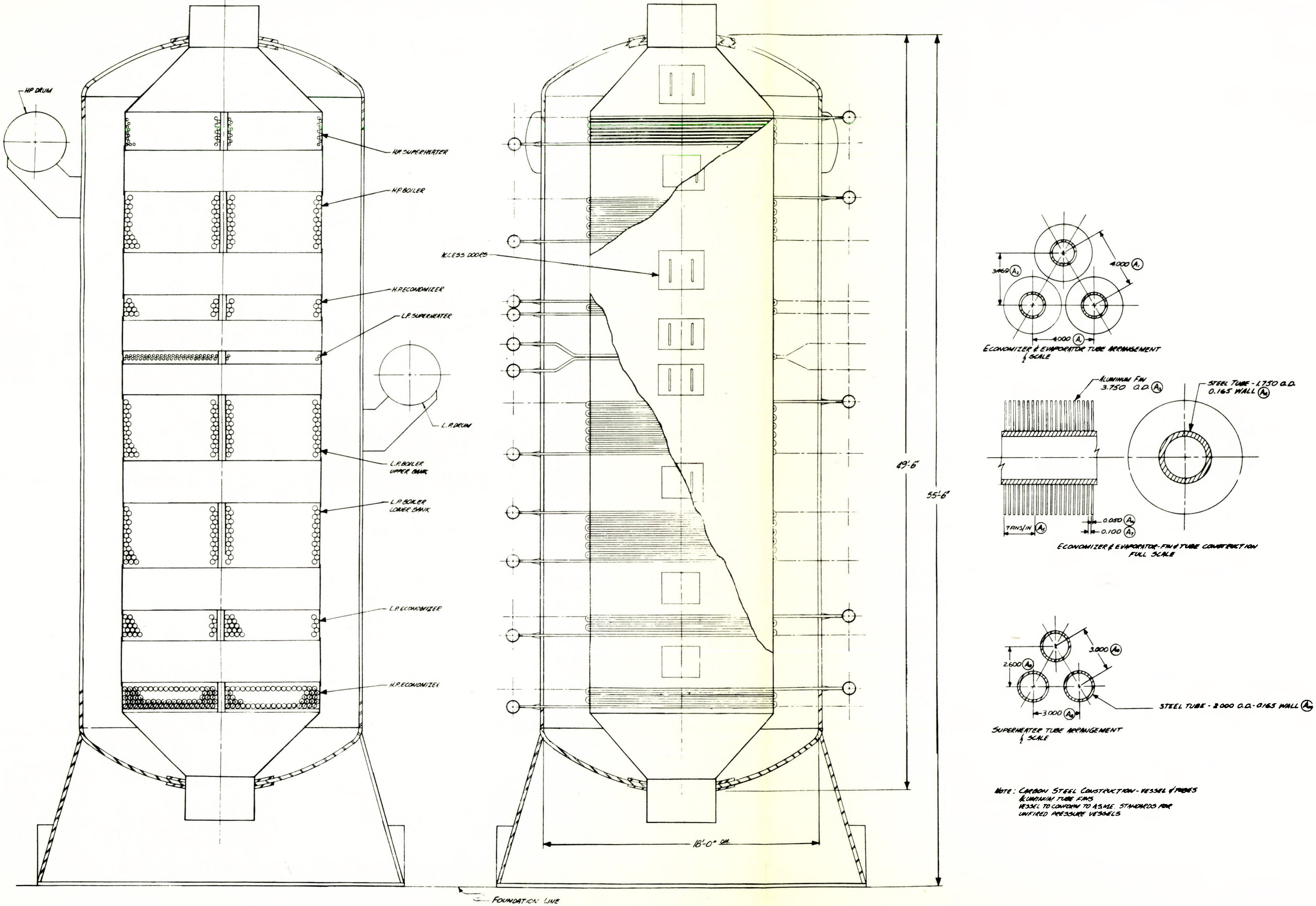


ELEVATION B-B

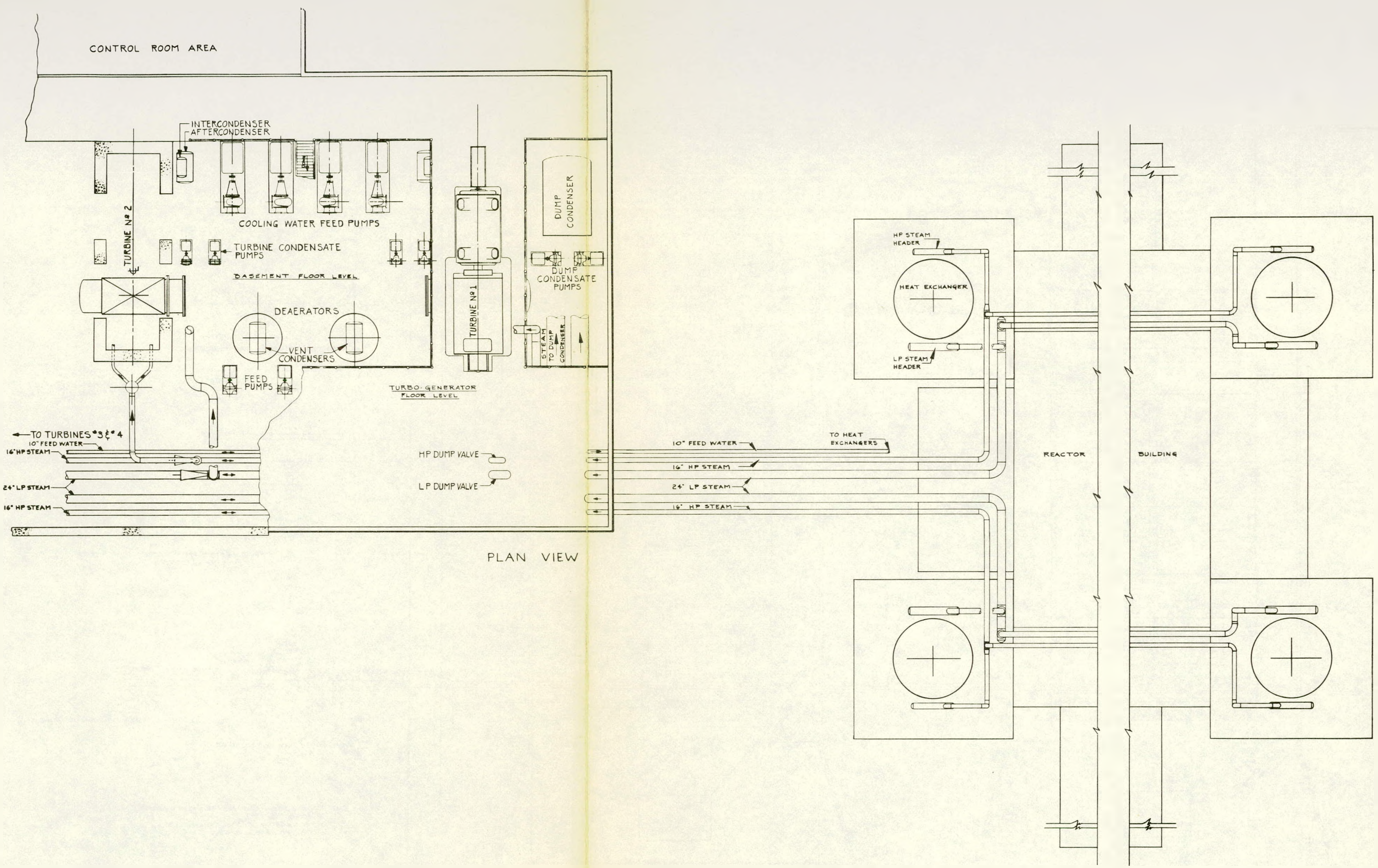
Dwg. 9. Heat Removal System - Piping
(AE70-978001)



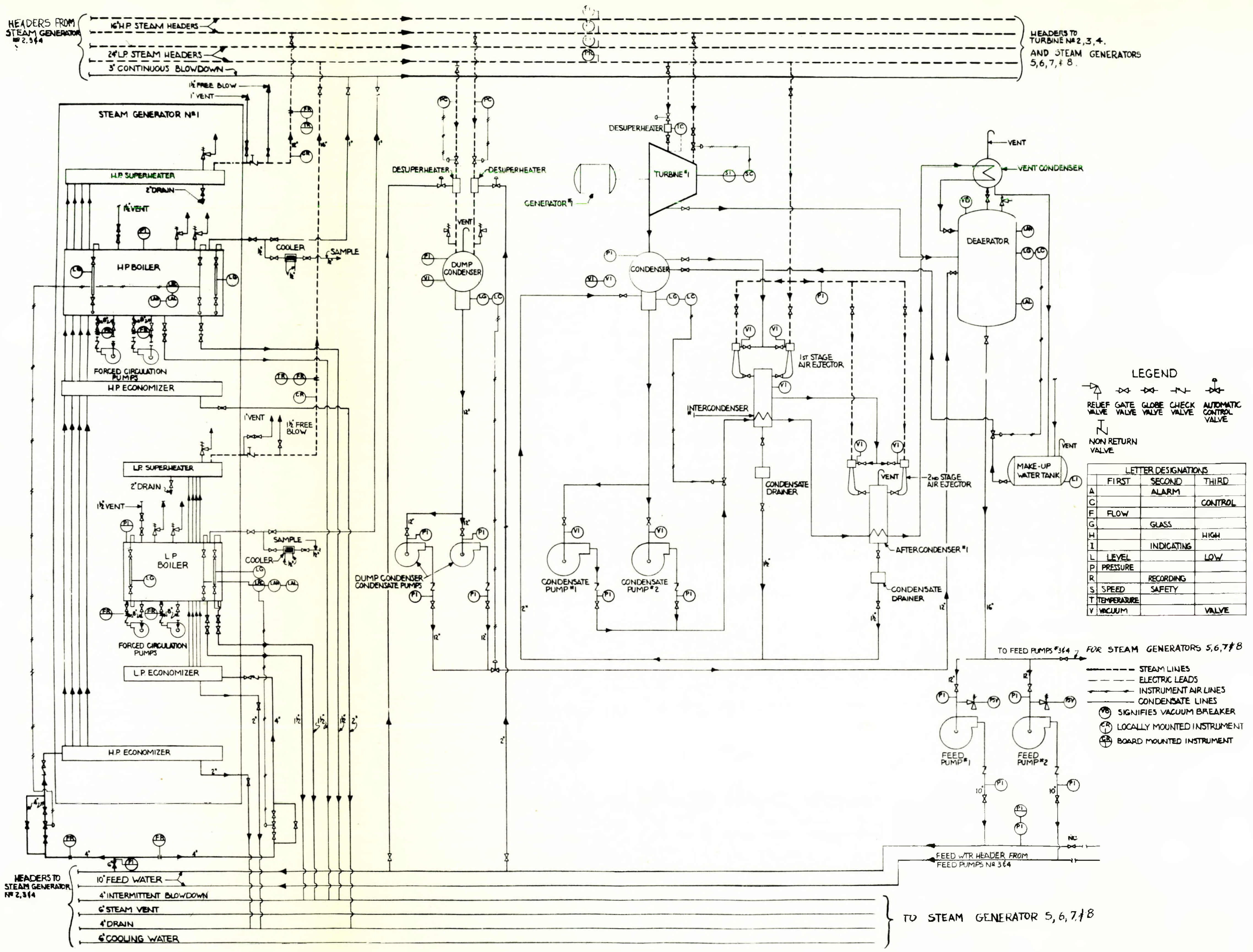
Dwg. 10. Heat Removal System - Flow Diagram (AE70-07001)



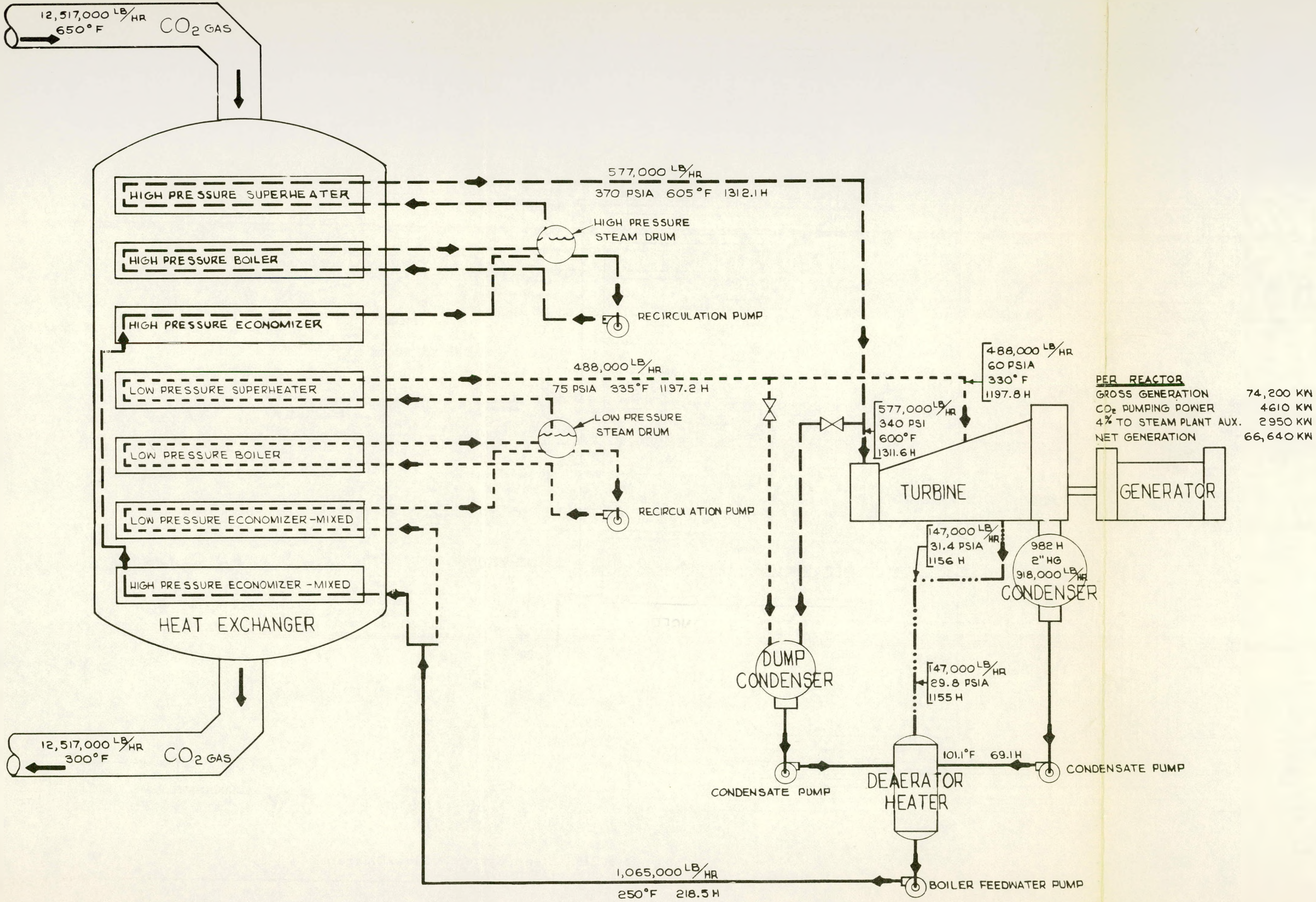
Dwg. 11. Steam Generator (AE70-978201)



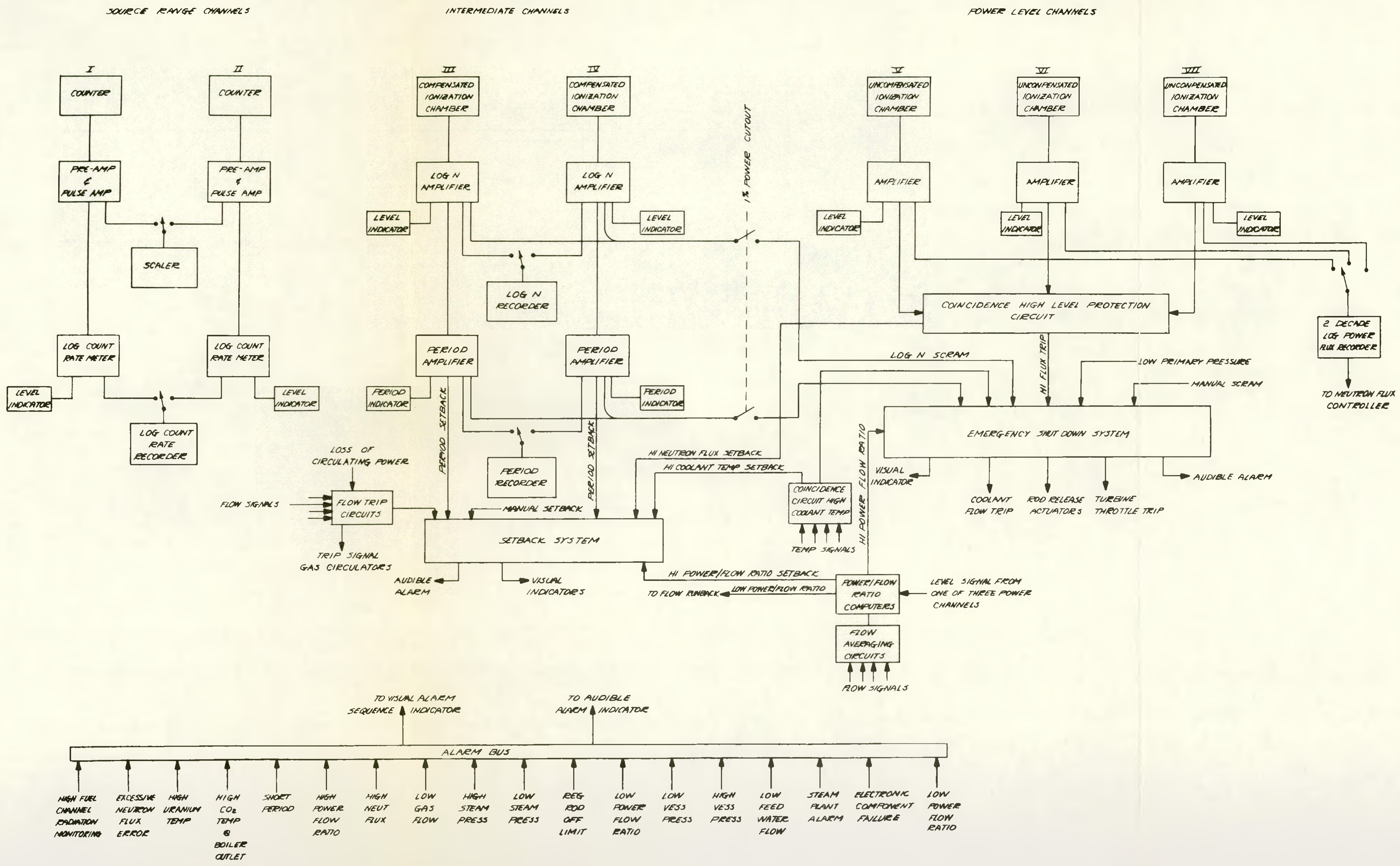
Dwg. 12. Steam System - Layout
(AE70-978002)



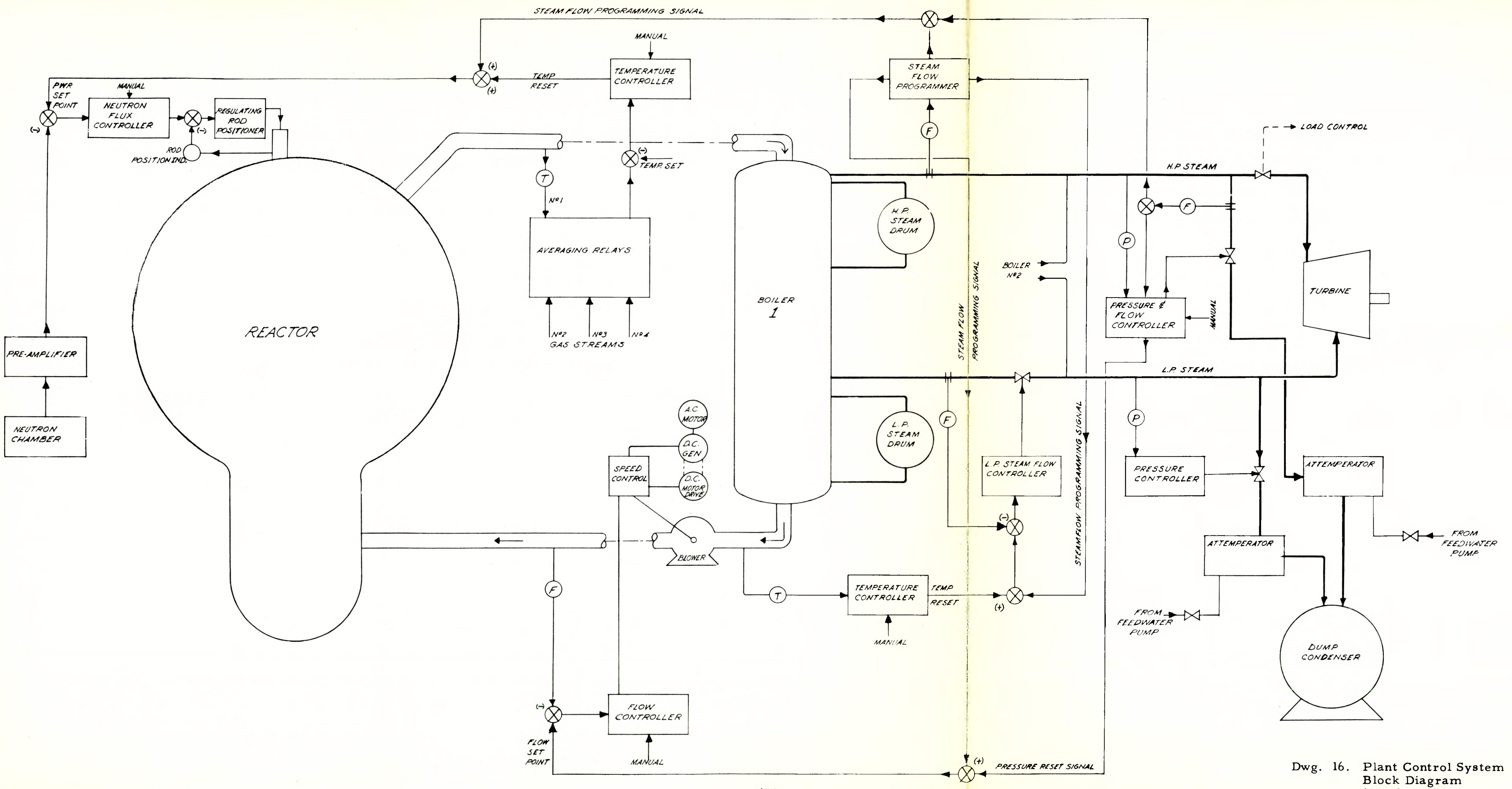
Dwg. 13. Steam System - Flow Diagram (AE70-07002)



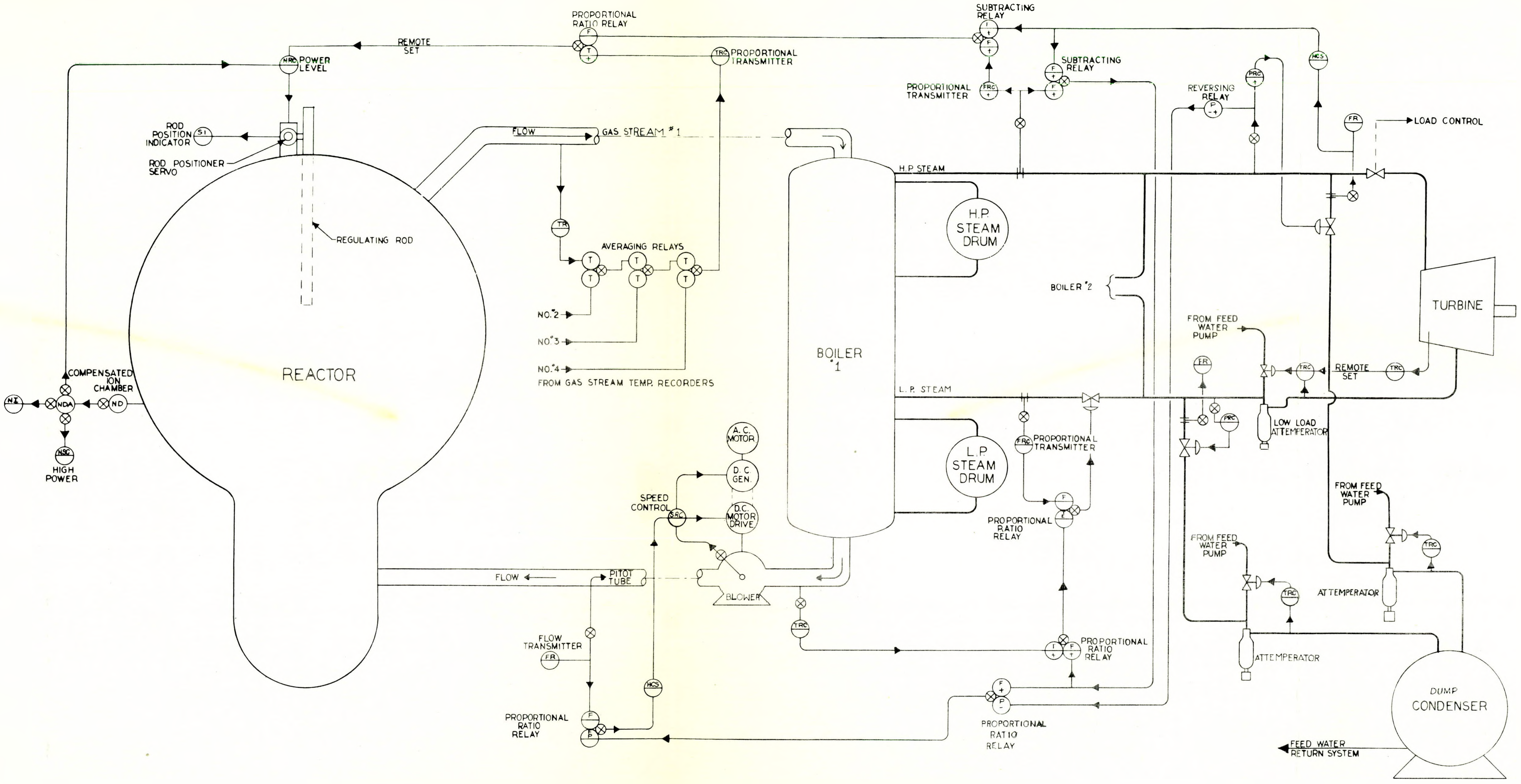
Dwg. 14. Heat Balance
(AE70-07003)



Dwg. 15. Nuclear Instrumentation (AE70-972001)



Dwg. 16. Plant Control System Block Diagram (AE70-972008)



MEASURED VARIABLE	INSTRUMENT FUNCTION				
	FIRST LETTER	SECOND LETTER		THIRD LETTER	
TEMPERATURE	T				
PRESSURE	P				
FLOW	F				
NEUTRON	N				
SPEED OR POSITION	S	S	SAFETY	S	STATION
HAND	H				
		R	RECORDING		
		C	CONTROL	C	CONTROL
		I	INDICATOR	A	AMPLIFIER
		D	DETECTOR		

Dwg. 17. Schematic Control Diagram
(AE70-972002)