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RIDGE SCHOOL OF REACTOR TECHNOLOGY

Reactor Design and Feasibility Problem

HIGH TEMPERATURE AQUEOUS

HOMOGENEOUS REACTOR

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OAK RIDGE SCHOOL OF REACTOR TECHNOLOGY

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F. C. VonderLage, Director

Reactor Design and Feasibility Problem

HIGH TEMPERATURE AQUEOUS HOMOGENEOUS REACTOR

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DATE FEB 15 1957 *ml*
For The Atomic Energy Commission
H. R. Cansell
Chief, Declassification Branch

Photostat Price \$ 22.80

Microfilm Price \$ 7.20

Available from the
Office of Technical Services
Department of Commerce
Washington 25, D. C.

August 1955

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In September, 1954, a group of men representing various scientific and engineering fields embarked on the twelve months of study which culminated in this report. For nine of these months, formal classroom and student laboratory work occupied their time. At the end of that period, these seven students were presented with a problem in reactor design.

This is a summary report of the study, the research, the problems and the solutions which developed during the final ten-weeks period of the school term. It must be realized that, in so short a time, a study of this scope can not be guaranteed complete or free of error. This "thesis" is not offered as a polished engineering report but rather as a record of the work done by the group under the leadership of the group leader. It is reproduced for use by those persons competent to assess the uncertainties inherent in the results obtained in terms of the preciseness of the technical data and analytical methods employed in the study. In the opinion of the students and faculty of ORSORT, the problem has served the pedagogical purpose for which it was intended.

As a matter of historical fact and pride we point out that similar investigations by student groups of previous ORSORT classes have led to sufficiently encouraging results to warrant more exhaustive studies; in at least one instance, a reactor first investigated by a student group is soon to become a physical reality. There is also recorded an instance in which calculations contained in a similar report were uncritically abstracted and applied to a study for which they were never intended. It is to avoid the recurrence of the latter experience that we have taken some pains to acquaint the reader with the character of this report.

The faculty wishes to join the authors in an expression of appreciation for the assistance which various members of the Oak Ridge National Laboratory have so generously contributed. In particular, the guidance of the group consultants, P. C. Zmola and C. D. Zerby, is gratefully acknowledged.

F. C. VonderLage

for

The Faculty of ORSORT

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ACKNOWLEDGMENT

This report could not have been completed without the assistance of many members of this Laboratory. Our advisors, P. C. Zmola and C. D. Zerby, initiated the problem and provided the overall guidance throughout the preparation of the report. T. A. Fowler, L. Dresner, M. Tobias and W. Kinney helped in obtaining the nuclear parameters and in running the ORACLE calculations. C. H. Secoy and W. L. Marshall provided the fuel solution information. R. H. Chapman helped with the thermal shield calculations. W. Ross, A. S. Kitzes and J. C. Griess helped with the pumping, slurry and corrosion problems. R. B. Korsmeyer provided help with the cost analysis.

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ABSTRACT

The feasibility of high temperature aqueous homogeneous reactors was investigated in this study. The physical requirements for a high temperature aqueous fuel were outlined.

One system, operating with a reactor outlet temperature of 1200°F., was studied in detail, and its costs were compared with those of a comparable low temperature system.

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CHAPTER 1 - SUMMARY

1.1 - Introduction

The incentive for investigating a high temperature aqueous homogeneous reactor system is the desire to determine if increased thermal cycle efficiency can result in a reduction in electric power generation costs.

It was recognized at the beginning of the study that a reactor system of this type would take many years of development, and that reasonable progress in component design and manufacturing techniques should be anticipated.

Actual cost information for reactor plants is very uncertain. Since an electric power generation cost comparison is necessary to reach a conclusion, it was decided to compare the costs with those of the TBR. Thus, all the plant costs were estimated on the same basis as the TBR. In this comparison it is necessary to point out that the TBR represents an optimized design, while the HTHR could not be optimized in the time allotted for this study.

1.2 - Reactor System

The TBR is designed to operate with an outlet temperature of 570°F. (300° C.) and achieves a net thermal efficiency of about 26%. The HTHR should then operate with an outlet temperature of about 1000°-1200° F. (550°-650° C.) in order to have a net thermal efficiency of 39 - 42%. The latter temperatures are in the supercritical region for aqueous systems. The operating pressures for this system must then be in excess of 3200 psi to avoid a phase interface and to keep the fluid density as high as possible.

To obtain a containment vessel of reasonable diameter at these pressures, it was necessary to assume that manufacturing techniques would be improved to the point where an 8-9 inch thickness of low alloy steel (similar to SA 3028) could be fabricated. This projection will permit the manufacture of a vessel with 10 foot inside diameter to withstand a design pressure of 4000 psi. (Figure 2.4-1)

The nuclear calculations revealed that a single pass, one region reactor is not feasible because the leakage of neutrons is excessive. This indicated the need for a low temperature outer region with a high temperature core. This arrangement could be accomplished with a one region multi-pass core or a two region reactor with a low temperature blanket.

It was postulated that for any reactor to be economic in a projected nuclear powered electric system, it must have a breeding ratio close to one. Thus, to keep the breeding ratio close to one, for a system with a relatively high neutron leakage, it is necessary to utilize the better breeding characteristics of the two-region reactor. The proposed blanket for this reactor is a ThO_2 slurry in heavy water operated in the normal density region around 570°F . (300°C .).

This system becomes essentially a reflector moderated reactor. However, with no moderation in the core region, it is not possible to obtain a critical system with reasonable core fuel concentration. Thus, the critical fuel concentration becomes very sensitive to the Fermi Age of the core fuel system.

Two group, two region nuclear calculations reveal that in order to keep the critical fuel concentration under 30 grams per liter, the core

fuel system must have an Age less than 4500 cm^2 . To help keep within these limits, and to maintain a high outlet temperature, the reactor had to be designed to operate with a large temperature rise through the core so that the average core temperature would be appreciably less than the outlet temperature. To avoid the rapid change in density near 700° F . it was necessary to limit the inlet temperature so that it was not less than 750° F .

The Age requirement eliminates the possibility of using heavy water for the core fluid at high temperature.

A UO_3 slurry in light water can be used up to an average temperature of about 950° F . This results in an outlet temperature of about 1100° F .

Another possible fuel system is UO_3 dissolved in chromic acid. Limited experimentation with this system indicates that it remains in solution in the supercritical region. A reasonable estimate of the form of this solution is $\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5 \text{ H}_2\text{O}$. The effect of the water of hydration, the large amount of oxygen, and the inelastic scattering of the chromium and uranium is to reduce the Age. This fuel system can be used up to an average temperature of 1050° F . with an outlet temperature of 1200° F .

Uranyl phosphate is another possible high temperature fuel. This fuel is being proposed by Los Alamos for the LAPRE, and was not considered in this study. Sufficient information on this reactor was not available to make a comparison with the HTHR.

The uranium dichromate system was studied in detail as it afforded the utilization of a 1200° F . outlet temperature and the corresponding high thermal cycle efficiency. The results of this system will apply,

in general, to any fuel system that can be devised to give the same Age at an average temperature of 1050° F. A Breeding Ratio of about 1.00 was obtained for this system. The neutron captures in chromium did not represent a large factor in this low breeding ratio. If the natural chromium were replaced with a low cross section chromium isotope, the breeding ratio would increase to only 1.01.

The limited amount of experimentation with the uranium dichromate system indicates that it is considerably less corrosive than the sulfate system to both stainless steel and carbon steel. The core and blanket fluid processing system are very similar to the sulphate processing system with the exception of the equipment for converting the chromate to a nitrate. The radiolytic decomposition of the water appears to be no problem. Experiments indicate that at the temperature we are considering, the recombination rate will equal the decomposition rate.

The gaseous fission products have a density that is close to the high temperature water density. Thus, there is no simple mechanism for gaseous fission product removal as there is in the low temperature reactor systems. To keep the Xenon poisoning to a reasonable value, the let down system capacity had to be increased to handle 29 core loop volumes per day.

From a control and efficiency standpoint, it appeared desirable to select a supercritical steam cycle. This system, because it utilizes two high pressure fluids, results in a great length of small diameter heat exchangers. This design produces high equipment costs and high fuel inventory. The boiling cycle, selected for this reactor, has a comparable efficiency and greatly reduces the heat exchanger problems.

The boiling cycle does introduce a control problem for partial load operation. At light load, the low temperature reactor systems permit the secondary pressure to rise to limit the heat transfer. Since this reactor operates above the critical temperature, pressure increase cannot be used to decrease the heat transfer. Thus, a means of reactor flow control must be established. This flow can be controlled by variable speed pumps or by valving. Valve design is very uncertain, but modification of existing canned rotor pumps will be possible to fit the pumping needs for this system.

The pressurizers for this system are somewhat different from those used for low temperature reactors. Their major function will be for start-up and steady state pressure control. Since the entire system is compressible and is all one phase, the pressurizer does not have to act as a surge volume. To satisfy the startup requirement, the pressurizer must be located at the low point of the reactor system.

1.3 - Costs

The costs for a 290 MW, two unit plant and a 435 MW, three unit plant for the HTHR were compared with the costs for a 300 MW, three unit plant for the TBR. All the cost evaluation was performed on the same basis, using the same estimating formulas. Table 1.3-1 is a summary of the power costs:

TABLE 1.3-1
COST COMPARISON

	<u>TBR</u>	<u>HTHR</u>	
Heat (MW)	1150	690	1040
Electric (MW)	300	290	435
Capital Cost (Mills/KWH)	3.81	4.03	3.97
Net Fuel Cost (Mills/KWH)	1.22	1.59	1.27
Operating Cost (Mills/KWH)	0.19	0.19	0.19
Maintenance Cost (Mills/KWH)	0.38	0.40	0.40
Total Cost (Mills/KWH)	5.60	6.21	5.83

The fuel costs were based on the costs for an integral fuel processing plant.

Two plant sizes for the HTHR were used to get a comparison of costs on an equal heat generation and an equal electric capability basis. The heat generation comparison will be important for site locations that have limited cooling water facilities.

1.4 - Design Uncertainties

Because of the lack of time or experimental evidence, it was necessary to assume successful solutions to many problems, any of which could determine the feasibility of this reactor system.

Very little information is available on the high temperature properties of the fuels that were considered. Little is known about the heat transfer and fluid flow properties of the slurry system. A big uncertainty for the dichromate solution is its radiation stability. The hydration properties of the dichromate solution and its limits of solubility had to be assumed.

The reactor kinetics for this system lead to equations that require machine solutions. No time was available for this study, and thus the design was assumed to be feasible from a safety viewpoint.

Many of the nuclear parameters had to be assumed or approximated. Before a design of this type could be considered, it would be necessary to make Fermi Age measurements. Fermi Age is the most sensitive nuclear parameter for this system. Data for U^{233} fast effects will also be necessary for better nuclear calculations since the core fast flux for this system is relatively high.

The high temperature, high pressure fluids have forced the component design to the limits of material properties and fabrication techniques. Items such as pressure and thermal transients have not been considered. A great deal of work on component research will be necessary before a reliable system to contain the high pressures and temperatures can be designed.

1.5 - Conclusions

If all the technical and economic problems involved in building a high temperature aqueous homogeneous reactor are solved, it will probably have the following characteristics:

- a. Two region or multipass arrangement with a low temperature outer region and high temperature core.
- b. Reactor will operate with a large temperature rise through the core.
- c. High fuel concentration will be necessary for high temperature criticality.
- d. Breeding ratio will be close to unity.

- e. Blanket power will be low and used for feed water heating.
- f. Fuel solution flow rate will vary with power demand.
- g. Blanket fluid will be heavy water.
- h. Core fluid will be light water.
- i. Large let-down system to control poison level of gaseous fission products.
- j. Net thermal efficiency will be about 40%.

The cost comparison with the TBR reveals that within the accuracy of cost estimating formulas, the capital cost for both plants are the same.

The net fuel cost variation reflects two important items. For equal electric capability the TBR fuel costs are lower because of the higher breeding ratio obtained in the TBR system. For equal heat generation capacities, the effect of breeding ratio is offset by decreased chemical plant fixed costs, which represent the major contribution to fuel costs for an integral plant.

An optimization of the HTHR, similar to that performed on the TBR, will probably show that for equal electric capability the power costs will be the same.

Thus, where cooling water facilities are not a limiting factor, there seems to be no advantage to building a complex HTHR. However, for a given cooling water capacity, the HTHR could be built with twice the electric capability of the TBR and with no increase in power costs.

CHAPTER 2 - REACTOR DESIGN

2.1 - Pressure Vessel and Thermal Shield

The proposed pressure vessel is to be made of a material similar to SA-302-B low alloy (manganese and molybdenum) steel, which has an allowable stress of 20,000 psi at temperatures to 750° F. (ASME Boiler and Pressure Vessel Code, p. 72.) The inner diameter of the pressure vessel is to be 10'-0", and the wall thickness, 8". Both the thickness and the material are projected into the future somewhat. Current practice limits the materials to an allowable stress of about 15,000 psi and the thickness to about 7". At present the Westinghouse Atomic Power Division is making a feasibility study of the SA-302-B steel, particularly in regard to neutron irradiation. It does not appear unreasonable that at a future date fabrication techniques will be improved to the point where an 8" thickness is feasible.

Allowing for a maximum core pressure of 4,000 psi, the mechanical tangential stress is 15,000 psi. Based on a calculation for a smaller pressure vessel, the thermal stresses are conservatively estimated to add about 3,000 psi. Using a material similar to SA-302-B, and allowing a 90% weld efficiency makes this design acceptable. $(15,000 + 3,000 = 20,000 \times 0.9)$

Gamma heating calculations were made for a vessel 8.42' in inner diameter and 9" thick, with a thermal shield of variable thickness. (See Appendix A). With the same core as for this case and a three-inch thermal shield, a tangential thermal stress of 4,500 psi was calculated for the smaller vessel. A power density of 150 KW/L in the core and 6.8 KW/L in the blanket was assumed. It was further assumed that the vessel was

cooled so as to maintain the same temperature on inner and outer faces in the radial direction, thus minimizing thermal stresses. In order to maintain a better neutron economy it was desired to increase the blanket thickness from 14.5" to 24", and use a higher strength steel. Since with a three-inch thermal shield the major portion of the pressure vessel heating is caused by gammas resulting from capture of thermal neutrons in the thermal shield, and since by going to the larger blanket the thermal neutron leakage is cut by a factor of .28, it is postulated that in going to the 10'-0" pressure vessel the thermal stress can be reduced from 4,500 to about 3,000 psi or less. Thus the proposed pressure vessel has a 3" thermal shield 9'-4" in inner diameter, with a 1" clearance between thermal shield and pressure vessel. Because of the corrosion-erosion problems connected with the blanket solution it will be necessary to line the pressure vessel and thermal shield with about $\frac{1}{4}$ " of 347 type stainless steel.

A maximum temperature in the pressure vessel of 645° F. was calculated for the case of the smaller vessel. A heat flux of 1,540 BTU/hr ft² was calculated for the outer surface. Since a blast shield must be built around the pressure vessel, it is not possible to relieve this heat to the ambient through three stagnant air films. Therefore the space between the pressure vessel and blast shield should be carefully cooled by forced convection. Perhaps a more realistic approach would be to add an inch or two to the thermal shield, and allow the outer surface temperature of the pressure vessel to rise, cooling by natural convection and radiation.

Core and blanket piping nozzles are concentric, so that the pressure vessel is in contact only with the blanket solution, which enters at 479

and leaves at 621° F. No attempt was made to calculate the thermal stress arising from the axial temperature gradient, but it is assumed that it is small in comparison with the stress due to the internal pressure and the radial temperature gradient. Similarly no attempt was made to evaluate stresses due to nozzle openings, support lugs, and hydraulic forces.

The proposed pressure vessel is shown in Fig. 2.1-1. See Appendix A for a more detailed description of the calculations.

2.2 - Core Shell

In order to maintain the breeding ratio, the core shell must have a low cross section to thermal neutrons. Zirconium was chosen for this reason and because of its adequate mechanical properties. The shell is 63" in inner diameter and $\frac{1}{2}$ " thick. With a 200 psi differential between core and blanket the tangential stress is 6,300 psi, tension or compression.

At the top of the core shell a radial temperature differential of 580° exists. It is assumed that the internal generation due to gamma attenuation is negligible in determining the temperature pattern. An approximately linear temperature profile in the zirconium with a total difference of about 250° was calculated, based on film coefficients obtained by the Colburn equation (McAdams, 2nd Ed. p 168). Properties of zirconium are taken from the Reactor Handbook, 111, p. 531-541. The thermal stress for a thin sphere can be approximated by

$$\sigma_{TH} \cong \frac{\alpha E}{1-\nu} \frac{\Delta t}{2}$$

PRESSURE VESSEL DETAILS

SCALE $\frac{1}{2}" = 1'-0"$

FIGURE 2.1-1

For this case the thermal stress amounts to 6,700 psi, making a total stress of 13,000 psi. Again no attempt was made to calculate the effect of stresses resulting from the axial temperature gradient, but again it is assumed that they are insignificant in comparison with those arising from the pressure and radial temperature differentials. Yield strengths for 5% tin alloys of zirconium can be extrapolated to about 27,000 psi at 1150° F., the calculated upper temperature of the shell.

The shell is flared at both ends in order to minimize axial thermal stresses and to minimize hydraulic friction losses in the core. Diffuser screens in the inlet help to insure an evenly distributed flow. The shell is connected to 347 type stainless steel inner pipes by means of a flanged joint at the top and a bellows-type expansion joint at the bottom. A maximum pressure differential of 200 psi between core and blanket is insured by a diaphragm, which should be located in a fairly accessible part of the upper spaces, in order to replace it readily in the event of a failure. It appears that the expansion joint is the weakest part of the barrier between core and blanket. HRT test data indicated that a bellows can safely withstand a differential of 200 psi at 600° F.

See Fig. 2.1-1 for a sketch of the proposed core and pressure vessels.

2.3 - The Reactor Fuel

The High Temperature Homogeneous Reactor will employ an aqueous fuel solution which will operate well above the supercritical temperature and with an outlet temperature of 1200° F. Nuclear calculations have shown that a uranium oxide slurry might be useable in a reactor with an outlet temperature up to 1100° F. The physical properties of slurries have not been determined above 300° C. and cannot be extrapolated up to the super-

critical region with any accuracy. Further study on the physical properties of slurry systems in the high temperature region will be necessary before this fuel can be evaluated.

A solution of uranyl sulfate and uranyl fluoride in H_2O or D_2O cannot be maintained at conditions above the critical temperature. The phase diagram for UO_2SO_4 and H_2O (RH-2, p. 647) shows the system to exist as two phases, an unsaturated solution and a vapor phase. The phase diagram for UO_2F_2 and H_2O (RH-2, p. 679) shows the system to exist as a solid phase in equilibrium with a vapor phase. Moreover, the fluorides have been found not to be compatible with the zirconium core shell in a two-region reactor.

The system $UO_3-CrO_3-H_2O$ has been explored by Secoy, Loprest, and Marshall (ORNL-1813, HRP Qtrly. Rep., Oct. 31, 1954). The system has been studied as a possible fuel solution for the HTHR based on the following advantages.

(1) The solubility of UO_3 in chromic acid solution is high, probably of the order of 500 to 1000 grams per liter at $100^\circ C$. The solubility remains high even at temperatures approaching the critical temperature of the system. The Cr:U ratio necessary to produce this high solubility is of the order of 2:1.

(2) The above mixtures of $UO_3-CrO_3-H_2O$ apparently are soluble in the supercritical fluid, and for the most part stable at least to $500^\circ C$. ($932^\circ F.$), thus offering a fluid which completely fills the container.

(3) The presence of the large amounts of chromate in the system, as in the case of many other aqueous systems containing small amounts of chromate, appears to confer a high degree of corrosion resistance even upon carbon steel. Corrosion studies indicate that the chromate system is 20 to

30 times less corrosive than the sulfate system to both stainless and carbon steel (See Section 3.1 on "Corrosion").

(4) The oxidizing power of the chromate ion, prevents the reduction of U(VI) to U(IV).

(5) Nuclear calculations indicate that a chromate solution is useable in a reactor with outlet temperature up to 1200° F.

There are several known or possible disadvantages which may limit the use of the chromate system.

(1) A small quantity of insoluble brick-red brown precipitate which has been observed may indicate that the chromium in the oxidized state is unstable at elevated temperatures and is undergoing partial reduction. However, experiments performed under oxygen pressure indicate that the oxygen might counteract any tendency toward partial reduction.

(2) The neutron-absorption cross section of natural chromium is 2.9 barns, compared with 0.49 barn for sulfur. However, the separated isotope Cr^{52} , occurring in 83.7% abundance, has a cross section of only 0.72 barn, and the Cr^{54} isotope, although only in 2.38% abundance, has a cross section of 0.006 barn. The use of the low-cross-section isotope Cr^{54} results in an increase in breeding ratio of only about 1%.

(3) The radiation stability of chromate under reactor conditions has not been established.

Another fuel solution that might be used in the HTHR is UO_3 dissolved in phosphoric acid solution. Los Alamos has selected (LAMS-1611) for their high temperature (430° C.) Power Reactor Experiment, a solution of 0.6 M UO_3 in 7.5 M H_3PO_4 . At this high concentration of H_3PO_4 , the solution no longer acts like an aqueous system but more like molten glass. The H_3PO_4 is really

the solvent rather than the water. The fuel solution for the LAPRE remains in the liquid state at operating conditions due to the high concentration of acid. The corrosion problem for the phosphate system is sufficiently great that all container walls in contact with the liquid must be covered with gold or platinum. This indicates that a complex core and blanket arrangement will be necessary for efficient breeding. The phosphate system was not studied further since Los Alamos has chosen it as their reactor fuel.

Since the $\text{UO}_3\text{-CrO}_3\text{-H}_2\text{O}$ system seems to offer the best properties for a reactor with an outlet temperature of 1200°F. , this fuel system was selected for further study. However, any fuel system that has similar properties at these temperatures could be used.

2.4 - Nuclear Calculations

At the outset of the study it was recognized that the construction of the necessary pressure vessel imposed a major restriction on the reactor to be proposed. A carbon steel vessel of about seven inches in thickness can be fabricated with equipment now in use; nine inches was chosen as a reasonable estimate of possibilities in the foreseeable future.

Fig. 2.4-1 shows the required wall thickness as a function of the vessel's internal diameter and internal pressure for SA-212-B and SA-302-B steels. Type SA-212-B was initially selected, since it is now being used for pressure vessels, while SA-302-B is still in the testing stage.

Concentration (gm U-233/liter) is also an important factor because of its direct bearing on economics as capital and fuel inventory costs; 150 gm U-233/liter was selected as the maximum in the calculations to decide the basic questions.

TABLE 2.4-1
CRITICAL SIZE FOR BARE SPHERICAL REACTOR FUELED
WITH UO_3 SLURRY IN H_2O and D_2O

(Basis of Calculation Explained in Appendix B.1)

Temp.	Press.	Moderator	Age	U-233 Conc.	Dia. of Sphere
800°F.	5000 psi	H_2O	448 cm^2	150	5.96 ft.
1050	5000	H_2O	2550	150	13.50
800	5000	D_2O	1650	150	12.80
1050	5000	D_2O	9540	150	25.80

Note: From Fig. 2.4-1 the maximum diameter fabricable for a 5,000 psi vessel is 7 ft. for SA-212-B and 8.4 ft. for SA-302-B.

It can be seen from this table that the large age of the fuel solution at high temperatures results in high fast leakage, thus requiring a pressure vessel that is larger than can be fabricated. Since age (λ) varies inversely as the square of the density, this large increase of age with temperature can be understood from Fig. 2.4-2, a plot of H_2O density vs. temperature. At about 750°F. the density falls off tremendously, thus markedly reducing the moderating properties of a given volume of H_2O . Similar curves for D_2O are not as well known, but they have the same shape as those for H_2O .

At this stage an H_2O -moderated reactor looked most hopeful, provided that the fast leakage could be reduced in order to bring the pressure vessel within the projected limit of nine inches in thickness shown in Fig. 2.4-1.

For any reactor to be competitive in the long term scheme of the

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UNCLASSIFIED

REQUIRED WALL THICKNESS VS. INTERNAL DIAMETER FOR SPHERICAL PRESSURE VESSEL

VESSEL WALL THICKNESS (IN.)

PROJECTED MAXIMUM
FABRICABLE THICKNESS

METAL TEMP - 650°F
DESIGN THERMAL STRESS - 4000 PSI

ALLOWABLE STRESS

SA 212 B	SA 302 B
17500 PSI	20000 PSI

VESSEL INTERNAL DIAMETER (FT)

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FIGURE 2.4-1
0-20 0-20

FIG. 1

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H₂O Density vs. Temperature

H₂O Density (gm/cm³)

Temperature (°F)

FIGURE 2.4-2

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power industry, it must have facilities for breeding or conversion, and have a breeding ratio close to unity. The economic feasibility of the system is based on the relatively low cost of the fuel. If fissionable material is burned without breeding, its cost will rise, and the fundamental advantage of nuclear fuel over fossil fuel will be lost.

Breeding can be accomplished by putting fertile material in the fuel solution, or by going to a two-region reactor with a fertile blanket. The fast leakage can be decreased by using a two-pass core with the cooler, high density fluid on the outside, or by using a two-region reactor with a high-temperature core and a cooler, denser blanket.

Even though the two-pass core leads to a simpler system, the higher breeding ratio attainable with the separate blanket outweighs the physical complications, and a two-region reactor was studied.

Eighteen cases of reactors using a UO_3 slurry in H_2O were run on the ORACLE, using the General Two-Group, Two-Region Code developed by T. B. Fowler. Core size (or blanket thickness, since the pressure vessel diameter was set at 10 ft. for 3000 psi), core temperature, and pressure were varied. A constant blanket composition of 500 gm ThO_2 /liter in H_2O at 550°F . was used.

In all of the succeeding two-region calculations the blanket temperature was used as the neutron thermal base.

A detailed description of the cases selected is given in Appendix B.2.

From Figs. 2.4-3 and 2.4-4 it can be seen that the thorium absorptions decrease as the core radius is increased (blanket thickness decreased), and critical concentration of U-233 decreases as the core radius increases. (There is an optimum core radius from a viewpoint of concentration. The

curves will reach a minimum at radii somewhat larger than those shown, and will then rise as leakage though the thinner blanket becomes dominant.)

Figs. 2.4-5 and 2.4-6 indicate that both thorium absorption and critical concentration increase with core age.

Thus a compromise between an acceptable breeding ratio and a practical critical core concentration must be reached. This compromise is effected by reducing the core age as much as possible, so that the core radius can be kept to a minimum.

The breeding ratios, however, in all cases shown in the curves are much too low to be of much practical use in a nuclear power economy.

The following table of neutron absorption per U-233 absorption locates the major waste of neutrons in the cases investigated.

TABLE 2.4-2

NEUTRON BALANCE (PER U-233 ABSORPTION)

Core: UO_3 slurry in H_2O at 300 psia

Blanket: 500 gm Th/liter, 1.5 gm U-233/liter, 550° F, 300 psia

Vessel: 108-0" inner diameter, 9" thick, SA-212-B steel

Case	Core Temp.	Core Radius	Critical Concent.	Fast Leakage	Thermal Leakage	Thorium Absorp.	Abs. in Blanket H_2O	Abs. in Core H_2O	Core Age
111	750° F	3.35 Ft.	9.35 gm/l	.1235	.0265	.366	.537	.156	2790
112	750	2.92	13.8	.0351	.0076	.432	.634	.104	2790
113	750	2.50	25.7	.0101	.0022	.466	.682	.055	2790
211	900	3.35	22.5	.1492	.0318	.406	.583	.044	5890
212	900	2.92	52.0	.0413	.0090	.472	.679	.019	5890
311	1050	3.35	64.3	.1610	.0343	.419	.594	.013	8690

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Thorium Absorptions vs. Core Radius

Thorium Absorptions per U₂₃₃ Absorption
FIG 24-3

480
460
440
420
400
380
360

2.4

2.5

2.6

2.7

2.8

2.9

3.0

3.1

3.2

3.3

3.4

Core Radius (ft.)

Core: UO_2 Slurry in H_2O , 3000 psi.

Blanket: 500 gm/l. ThO_2 in H_2O , 550°F, 3000 psi.

Pressure Vessel: 10 ft. dia.

$\tau = 2790 \text{ cm}^2$

$\tau = 5890 \text{ cm}^2$

$\tau = 8690 \text{ cm}^2$

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Critical Concentration vs. Core Radius

Core: UO_2 Slurry in H_2O , 3000 psi.

Blanket: 500 g/ml. ThO_2 in H_2O , 550°F, 3000 psi.

Pressure Vessel: 10 ft. dia.

Critical Concentration (gm/liter)

120
100
80
60
40
20

2.4 2.5 2.6 2.7 2.8 2.9 3.0 3.1 3.2 3.3 3.4

Core Radius (ft.)

750°F, 3000 psi, $T = 2790$

900°F, 3000 psi, $T = 5890$

1050°F, 3000 psi, $T = 8690$

FIGURE 2.4-4

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Thorium Absorptions vs Core Age

5-22-51 Thorium Absorptions per U²³³ Absorption
FIG 24-5

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Critical Concentration vs. Core Age

Core: UO_2 Slurry in H_2O , 3000 psi.

Blanket: 500 g/ml ThO_2 in H_2O , 550°F, 3000 psi.

FIG. 24-6

The above neutron balance indicates that the breeding ratio could be considerably improved by decreasing the H_2O absorptions in the blanket and the core. This parasitic absorption can be decreased by replacing the H_2O with D_2O .

The original calculations showed that the age of a UO_3 slurry in D_2O at the temperatures of interest makes the critical concentration too high. If the UO_3 slurry is replaced by a solution having water of hydration attached to the solute molecules, the age will be lowered because the moderator density is effectively increased. Uranyl dichromate was selected as a promising possibility for a fuel solution. In the chromate solution the age is also lowered by the high energy inelastic scattering of chromium and the elastic scattering of oxygen.

There is some question about the exact amount of water of hydration in this solution, but it was estimated that it is between five and ten molecules of water per molecule of uranyl dichromate. The following nuclear calculations with the chromate fuel are based on the conservative estimate of five, upon the suggestion of W. L. Marshall.

Core ages for the solution were calculated, accounting for the moderation by the fuel and its water of hydration. Displacement of solvent (D_2O) by the fuel was not taken into account. The method used to calculate age is outlined in Appendix B.3. Results were plotted in Fig. 2.4-7 as fuel concentration vs. core age for temperatures of 750, 900, and 1050° F.

Fig. 2.4-8 is a plot of age vs. U-233 concentration (gm U-233/kg D_2O), with actual heavy water density (accounting for displacement by the fuel) as a parameter. This curve can be used to determine ages of solutions at

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Fuel Concentration vs. Core Age

$UO_2Cr_2O_7 \cdot 5D_2O$ in D_2O (99.84% pure,
0.16% H_2O)

Blanket: ThO_2 in D_2O , 550°F, 3500 psia,
15" Thick.
Core Radius: 15.75"

Fuel Concentration (gm U^{233} /liter)

Age (cm^2)

FIGURE 2.4-7

Calculated Age of $UO_2Cr_2O_7 \cdot 5D_2O$ in D_2O (99.84% pure,
0.16% H_2O)
vs U^{233} Concentration
(D_2O Density as Parameter)

FIG. 2.4-8

conditions other than those shown in Fig. 2.4-7.

The next logical step in the development of an acceptable reactor appeared to be a parameter study using the core age as the independent variable for a system with a chromate solution as the fuel. Twelve cases were chosen for calculation with the two-group, two-region routine on the ORACLE. A detailed description of these calculations is outlined in Appendix B.4. Some of the cases (500 gm Th/liter) were hand-calculated for the condition of no slowing down in the core ($\tau = \infty$). The calculations indicated that it was impossible to obtain a critical system. Thus it became apparent that core age is a critical parameter to vary.

Curves of critical concentration vs. core age for the above twelve cases were superimposed on Fig. 2.4-7. The intersections of the two sets of curves locate the critical operating conditions for various reactors (i.e. at 750° F and 500 gm Th/liter in the blanket, the critical concentration is about 40 gm U-233/liter).

It can be seen from Fig. 2.4-7 that for the two-region reactors the critical concentrations are practical for core ages up to about 5000 cm². With a D₂O core, this age is obtainable only at a relatively low temperature (750°). This problem can be mitigated by going to an H₂O core to get smaller migration area, and thus decrease the core age. The use of H₂O is, of course, contingent upon the increased parasitic absorption not being large enough to reduce appreciably the breeding ratio.

To estimate the concentration that might be expected in going to an H₂O core and D₂O blanket, the age calculations for the solution (UO₂Cr₂O₇·5D₂O) were modified to account for the use of light water. The method is described in Appendix B.5.

Fig. 2.4-9 is a plot of these ages adjusted for light water vs. concentration, with the curves of critical concentration vs. age superimposed. For this case an age of 5000 cm² can be realized at a high temperature (1050° F) with reasonable concentrations. Thus a light water core appears to be a definite possibility, providing that the moderator absorptions were not too high.

The following neutron balance for the D₂O core reactors is included in an effort to evaluate the importance of the parasitic absorptions in the core.

TABLE 2.4-3
NEUTRON BALANCE (PER U-233 ABSORPTION)

Core: UO₂Cr₂O₇.5D₂O in D₂O at 3500 psia

Blanket: 550° F., 3500 psia, D₂O

Case	Core Age	Thorium Concent.	Critical Concent.	Fast Leakage	Thermal Leakage	Thorium Absorpt.	Abs. in Blank.D ₂ O	Abs. in Core D ₂ O
411	500 cm ²	500 gm/l	2.68 gm/l	.154	.105	.892	.00441	.00845
412	2000	500	10.0	.214	.109	.858	.00352	.00126
413	3500	500	26.1	.238	.111	.844	.00316	.00150
414	5000	500	96.0	.253	.111	.833	.00292	.000075
421	500	1000	3.11	.1595	.0359	.972	.00203	.00690
422	2000	1000	18.8	.230	.0403	.925	.00141	.00063

It is evident from the above neutron balance that the thorium absorptions (and thus the breeding ratio) are approaching practical values, but that the leakage terms are still appreciable. The core moderator absorptions are very small, so it would appear that the use of H₂O in the core at the low densities involved would still result in tolerable parasitic absorptions.

The leakages can be reduced and the breeding ratio improved by using

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Fuel Concentration vs. Core Age $UO_2Cr_2O_7 \cdot 5H_2O$ in H_2O

Blanket: ThO_2 in D_2O , 550°F, 3500 p.s.i.,
15" Thick
Core Radius: 15.75"

Fuel Concentration (gm U_{235} /liter)

Age (cm^2)

FIGURE 2,4-9

a thicker blanket. If the blanket is made thicker by maintaining the pressure vessel diameter and decreasing the core radius, the critical concentration will be too high. The previous core radius (2.62') can be retained and the blanket thickened by assuming that the low alloy steel, SA-302-B, will prove suitable for use as a vessel. With SA-302-B steel, a vessel 10.5 ft. in diameter and 9 in. thick can be fabricated to withstand a pressure of 4000 psi. See Fig. 2.4-1.

Four cases were selected for calculation on the ORACLE, using $\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5\text{H}_2\text{O}$ in H_2O in the core and a ThO_2 slurry in D_2O as the blanket. Core age was again used as the independent variable over the range of 3000 to 4500 cm^2 . Blanket thickness was increased to 24 in. (61 cm). A detailed description of these four cases is included as Appendix B.6 (Cases 511, 512, 513, and 514).

The critical concentration curve for the four cases is plotted in Fig. 2.4-10 (500 g. Th/liter). Thus, the water moderated core at 1050° F is critical at about 30 g. U-233/liter.

TABLE 2.4-4

NEUTRON BALANCE

Core: $\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5\text{H}_2\text{O}$ in H_2O at 1050° F, 3500 psia, 2.62 ft. rad.

Blanket: ThO_2 slurry in D_2O , 550° F, 3500 psia, 1.5 gm U-233/liter

Total U-233	1.000
Fast Leakage	.0788
Thermal Leakage	.0464
Shell	.0102
Core H_2O	.0346
Cr	.0130
U-234 and F.P.	.0630
Blanket D_2O	.0041
Th	1.025
Pa	.0222
U-234 and F.P.	.0031

$$2.300 = \eta \text{ for U-233}$$

Breeding Ratio:

$$\text{B.R.} = \frac{\text{Th absorptions} - \text{Pa absorptions}}{\text{Total U-233 absorptions}} \quad \text{B.R.} = \frac{1.025 - .022}{1.000} = 1.003$$

(see note top page 43)

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Fuel Concentration vs Core Age

FIG. 2.4-10

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Note: In the neutron balance it is assumed that fission in U-235 produces enough neutrons to cancel out the absorptions in the higher isotopes of uranium.

The additional critical concentration curves in Fig. 2.4-10 (600 g. Th/liter and 700 g. Th/liter) are the results obtained from calculations with these increased thorium concentrations. Fig. 2.4-11 shows the variation of breeding ratio with core age for critical systems at 1050° F and 900° F.

Thus, from Figs. 2.4-10 and 2.4-11, it can be seen that the breeding ratio can be increased by increasing the thorium concentration in the blanket without going above practical uranium concentrations in the core. The desirability of doing this would be determined by an economic optimization study.

2.5 - Safety

An attempt was made to get an indication of the reactivity change with a change in moderator density (core age). Cases 511, 512, 513, and 514 were rerun on the ORACLE with γ of the core equal to 1.9, 2.3, 2.5 instead of the actual value of 2.1 (other parameters for cases 511, 512, 513, and 514 are given in Appendix B.6). The resulting critical concentrations are plotted in Fig. 2.5-1.

From these curves, an approximate coefficient of reactivity was obtained:

$$\frac{\Delta K}{\Delta \rho} = 4.2/\text{gm.}/\text{cm}^3$$

For a moderator density change at constant pressure, this can be interpreted as a temperature coefficient of reactivity of $-5 \times 10^{-4}/^\circ\text{F}$

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Breeding Ratio vs
Core Age at
Critical Conditions

Breeding Ratio

FIG 2.4-11

1.03

1.02

1.01

1.00

0.99

0.98

0.97

0.96

3200

3600

4000

4400

Core Age (cm²)

900°F, 3500 psi

1050°F, 3500 psi

or $-9 \times 10^{-4}/^{\circ}\text{C}$.

The kinetics of the system were not investigated. The fluid fuel is completely compressible so that pressure waves may be a major consideration in this analysis. Thus, a simple continuity condition cannot be used and the equations describing transient behavior become both time and space dependent.

Because of experience of the homogeneous projects thus far, and since this reactor exhibits a negative temperature coefficient, it is believed that the system would be as stable as a low temperature homogeneous reactor.

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Fuel Concentration vs. H_2O Density

(Showing various values of multiplication)

FIG 25-1

2.6 - Basic Data

Reactor Characteristics for Preliminary Design of a 290 MW (Electrical Output)

Station Employing Two Reactors

Electrical Capability per Reactor, MW	145
Gross Station Efficiency, %	45
Net Station Efficiency, %	42
Reactor Operating Pressure, psia	3500
Superheated Steam Temperature, °F	1100
Superheated Steam Pressure, psia	2350
Reheated Steam Temperature, °F	1050
Reheated Steam Pressure, psia	418
Condenser Vacuum, Pressure, Hg	1.5

<u>Item</u>	<u>Core</u>	<u>Blanket</u>
<u>Reactor Construction</u>		
Material	Zirconium 10% tin	SA-302-B low alloy steel, lined with 347
Thermal Shield Thickness, in.	--	3.0
Space Between Thermal Shield and Vessel, in.	--	1.0
Pipe Connections	See Fig. 2.1-1 and	Table 3.7-1
Inside Diameter	5'-3"	10'-0"
Thickness	1/2"	8"
Blanket Thickness	--	24"
Volume, liters	2,140	9,760
<u>Reactor Systems</u>		
Operating Temperatures °F (C)		
Average	1,050 (566)	550 (288)
Inlet	750 (399)	486 (252)
Outlet	1200 (649)	621 (327)
System Volume, liters	9,030	12,740
Fluid Composition	UO ₂ Cr ₂ O ₇ .5H ₂ O in H ₂ O	ThO ₂ in D ₂ O (U)
U-233	30 gm/liter	1.5 gm/liter
Th-232	--	500 gm/liter
Total poison fraction (incl. 24, 13 and F.P.)	.075	.04
Inventory (2 reactors, not incl. Thorex plant)		
D ₂ O, #	--	44,500
U-233, kg.	691	38.4
Th-232, kg.	--	12,800
Heat Removal, MW (2 reactors)	600	90
Avg. Reactor Power Density, KW/liter	140	4.6
Power Density, External System	43.6	15.1

<u>Item</u>	<u>Core</u>	<u>Blanket</u>
Gross Breeding Ratio	--	1.003
Gross U-233 Produced, gm/day	--	1.82
Net U-233 Produced, gm/day	--	0.0
Circulating Rate, #/hr/reactor	2.348×10^6	1.660×10^6
Maximum Velocity, ft/sec.	173	20
Letdown Rate Required to Hold Xe Poison to 1%, core loop vol./day	29.1	--

Chemical Processing Plant

Processing Rates (2 reactors)		
Fluid		
U-233, gm/day	1704	426
Th-232, kg/day	--	142
D ₂ O, #/day	500	--
Cooling Period, days	90	65
Thorex U-233 Inventory, kg	187.4	36.4
Thorex th-232 Inventory, kg	--	12,080
Thorex D ₂ O Inventory	--	--

CHAPTER 3 - SYSTEM DESIGN

3.1 - Corrosion

Several static corrosion tests have been made (ORNL-1813, HRP Qtrly. Rept., Oct. 31, 1954, p. 171) on mild carbon steel (SAE1010) and type 304 stainless steel in both $\text{UO}_2\text{SO}_4\text{-H}_2\text{O}$ and $\text{UO}_2\text{Cr}_2\text{O}_7\text{-H}_2\text{O}$ systems with and without added oxygen. Both the carbon steel and the stainless steel showed a remarkable resistance (at 510°F) to corrosion in $\text{UO}_2\text{Cr}_2\text{O}_7\text{-H}_2\text{O}$ solution both in the absence and presence of added oxygen. In contrast, both carbon steel and type 304 stainless steel were corroded severely in $\text{UO}_2\text{SO}_4\text{-H}_2\text{O}$ solution in the absence of oxygen. The added oxygen did not protect the carbon steel from attack by UO_2SO_4 but, rather, increased the corrosion rate. No other corrosion data on the $\text{UO}_2\text{Cr}_2\text{O}_7\text{-H}_2\text{O}$ steel systems were found, but the above information seems to indicate that the corrosion problem would not be difficult to solve.

The high corrosion resistance of carbon steel and type 304 stainless steel to $\text{UO}_2\text{Cr}_2\text{O}_7$ solutions is apparently due to the high chromate ion concentration in the system. The rate and form of attack of both UO_2SO_4 and $\text{UO}_2\text{Cr}_2\text{O}_7$ on the zirconium core shell are believed to be similar (Private communication with J. C. Griess). Data seem to indicate that the corrosion rate of the zirconium core tank is primarily a function of power density rather than solution concentration or temperature.

3.2 - Radiolytic Gas Recombination

The fuel solution will pass through the core at 3500 psi, with an average temperature of 1050°F . At these conditions, it is believed that internal recombination of the radiolytic gases, without the use of an added catalyst, will be as rapid as production. Los Alamos reports

(LAMS-1611, p. 30) that in their LAPRE, recombination of the radiolytic gases by back reaction will be equal to the decomposition rate. Experiments indicate that total pressures, without an added catalyst, are not over 5500 psi for temperatures ranging from 650 to 800° F. Since the back reaction increases with increase in temperature, this pressure will be lower for an average solution temperature of 1050° F. Copper ion might be added to the fuel solution as a catalyst if additional recombination is needed. However, since light water will be used in the core system, any unrecombined H₂ and O₂ gases could be removed with the fission product gases. It is planned that radiolytic gases in the blanket loop be recombined internally by addition of a catalyst such as silver oxide to the slurry.

3.3 - Fuel and Blanket Processing Systems

3.3.1 - Process Description - Fission product removal from the fuel solution would probably be similar to that planned for the HRT. Since the core solution will be circulated in the supercritical region, separation of the fission product gases directly from the core loop may be difficult, if not impossible, by means of a mechanical centrifuge or cyclone. A more likely method seems to be to take a side stream from the low-temperature side of the primary loop and reduce the temperature and pressure of the fluid down to liquid phase formation by means of a heat exchanger and pressure reducer. The fission product gases could then be easily separated in a phase separation tank.

In order to limit the poison level of non-gaseous high-cross-section fission products in the active region of the reactor, continuous procession of the core solution will be required. If the fission product dichromates, especially the high-cross-section rare earths, are quite insoluble, they

might be removed by centrifugation, using the DorrClone principle of solids concentration and separation.

The Thorex processing plant will be required to control the level of soluble irradiation and corrosion products in the active region of the reactor and to permit recovery of the U-233 formed in the blanket. If the rare earth dichromates in the core solution are not sufficiently insoluble to warrant use of hydraulic cyclone separators, then the poison level will be maintained by cycling the core solution through the Thorex plant. Processing of the core solution by Thorex alone, without the use of hydraulic cyclones, requires a shorter chemical processing period to maintain the same poison level in the core. This results in (1) an increase in uranium inventory, (2) an increase in the volume of solution handled per day, and (3) a greater load on the U-233 isolation laboratory. Studies on the HRT processing plant have shown (ORNL-1761, p. 16) that incorporation of hydraulic cyclone separation with Thorex processing decreases the total fuel cost by about 6%. For the purposes of calculating processing rates and costs, it was assumed that separation of non-gaseous poisons by hydraulic cyclones is not applicable to the HTHR system and all processing is done through the Thorex plant. This should result in a conservative estimate of the fuel cost.

Prior to processing the blanket slurry, the D_2O will be recovered by evaporation and drying, and the beta activity allowed to decay to a permissible level. The cost of D_2O recovery has been estimated at approximately \$0.35 per liter when the recovery plant is integrated with the Thorex plant. After a total of 65 days cooling, the $ThO_2(H_2O)$ slurry will be transferred to dissolvers and converted to $Th(NO_3)_4$ by excess concentrated nitric acid containing flouride ion as catalyst.

Before the 90-day-cooled light-water core solution can be processed, a preparation step will be required. The Cr^{+6} ion, in the dichromate complex, will be reduced to the cation Cr^{+3} state by addition of the ferrous ion, probably as $\text{Fe}(\text{NO}_3)_2$, or similar reducing agent. Additional nitrate ion as $\text{Al}(\text{NO}_3)_3$, and acid or base will be added as needed for optimum feed concentrations. The nitrate core solution is next added to the blanket dissolver solution and prepared as the aqueous feed to the continuous solvent extraction process.

The aqueous solution from the thorium stripping column of the Thorex process containing the $\text{Th}(\text{NO}_3)_4$ is concentrated, and the thorium precipitated from the solution as thorium oxalate by addition of oxalic acid. The oxalate is then dried and decomposed to thorium oxide, which is used for remote re-preparation of a D_2O slurry for return to the reactor blankets.

The aqueous solution from the uranium stripping column containing the uranium product is processed by ion exchange for recovery and isolation of the U-233. This product is eluted from the ion exchange resin as uranyl acetate. The acetate is then converted to $\text{UO}_2\text{Cr}_2\text{O}_7$ and prepared in H_2O solution for return to the reactor cores. Since the breeding ratio for the HTHR is slightly greater than unity, it has been found that the small amount of U-233 gained in breeding just makes up for that lost in chemical processing.

3.3.2 - Fission Product Removal from Reactor Core - The fission product poison level in the reactor core depends upon the removal rate of the rare gases and the chemical processing rate of the core solution. In determining the processing rates required to hold the poisons at a given level, the fission product poisons were assumed to fall into three groups: (1) Those occurring as gases, including the high-cross-section gas Xenon-135. (2) Those remaining

species of relatively low cross section. These constitute the bulk of the total and are, in general, assumed to be converted by beta decay or neutron absorption into another species of low cross section. (3) The non-gaseous species with larger cross-sections. These species are assumed entirely destroyed upon decay or neutron capture.

About 30% of all fission products (i. e., 0.6 atom per fission) decay through rare gas isotopes. Of the rare gases, Xenon-135 by far contributes the greatest poisoning effect. For this reason, calculation of the rare gas removal by chemical processing was considered to be negligible. Thus, there will be left about 1.4 Group (2) plus a small fraction of Group (3) fission fragments in the reactor per fission. The following values for total fission yields "y" and average cross sections " σ " were selected (CF-54-4-198, p. 11):

<u>Group</u>	<u>Total Fission Yield</u>	<u>Average Cross Section @550° F.</u>
1	$y_X = 0.061$	$\sigma_X = 2.4 \times 10^6$ barns
2	$y_L = 1.4$	$\sigma_L = 35$
3	$y_H = 0.015$	$\sigma_H = 35 \times 10^3$

The cross sections of Groups (2) and (3) were obtained by applying a $1/v$ correction for temperature to the values given in the above reference. In using Group (3) values it was implicitly assumed that the half-lives of these species are long compared to the processing period. Corrosion products could be added into Group (2), although this was not done here.

For a given poison level, both Xenon and core solution removal rates are dependent on each other. For a given total poison fraction, the non-gaseous poison level must be decreased as the Xenon poison fraction is allowed to increase. This corresponds to an increased chemical processing

rate or a decreased processing period. No attempt was made to optimize the processing rates and the allowable Xe-135 poison fraction was chosen as 0.01. Using the above conditions and assumptions, the gas removal rate and the chemical processing rate were calculated (See Appendices C.1 and C.2). The contents of the core volume will have to be cycled through the gas separation system 156 times a day for removal of Xenon and through the chemical plant once every 76 days.

3.3.3 - Processing of the Blanket Slurry - In the two-region breeder, the blanket slurry is continuously removed from the reactor for processing. Fresh slurry, not necessarily containing the same amounts of the various materials, is fed in. The processing rate, required to hold the U-233/Th-232 ratio at a chosen value of 3 gm/Kg, has been calculated and found to be 0.0146 of a blanket volume per day or a blanket volume once every 68.5 days (See Appendix C.3).

3.4 - Choice of Thermodynamic Cycle

The choice of thermodynamic cycle presented many interesting problems. It was probably quite natural that the first cycle to be considered was one employing steam at greater than critical pressure with turbine throttle conditions of the order of 4000 psi and 1150° F. Such a cycle promises thermal efficiencies of the order of 44% with one reheat, to 46% with two reheats. Aside from efficiency, however, several other basic considerations become important, among which are practicability of design and fuel inventory requirements.

Further consideration of the critical pressure cycle demonstrated that heat exchanger design was a major problem. Preliminary calculations showed that heat exchangers designed to contain 4000 psi, both inside and outside

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the tubes, are of questionable practicability, and result in very high fuel hold-up. Simply stated, it is almost impossible to design a heat exchanger with a pressure shell to withstand 4000 psi and temperatures of the order of 1100°F, and at the same time to keep the physical proportions of the device within reason. Consideration was given to the use of several heat exchangers in series, as well as in parallel, in order to tailor each exchanger to the job to be done. In spite of these efforts, fuel hold-up remained high. Section 3.5 explains this problem in more detail.

The cycle finally chosen was that shown on Figures 3.4-1 and 3.4-2. Throttle conditions are 2350 psi, 1100°F, and reheat to 1050°F. It might be pointed out that this cycle is essentially the same as one which is employed in a modern fossil fuel burning plant, recently constructed in Northeastern U.S.A. The thermal efficiency of the cycle is 42%, which compares favorably to the efficiency of the critical pressure cycle mentioned previously.

It should not be inferred, however, that the cycle chosen is the best from all viewpoints. It was chosen mainly because it was known that this cycle was already in use in a conventional power plant, and many of the pertinent data were available. It may well be that a straight through cycle, employing moisture removal, would improve the over-all economics compared to the reheat cycle. The non-reheat cycle would obviously eliminate the need for the relatively expensive reheaters and might well lead to reduced fuel hold-up with little reduction in efficiency. An optimization study would be required to demonstrate definitely whether or not reheat should be used. It was decided, here, to use the reheat cycle in order to take advantage of the information already available.

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Compared to the critical pressure cycle, the cycle chosen mitigated considerably the heat exchanger design problem and led to a reduction in the amount of fuel hold-up and heat exchanger cost.

The choice of the lower pressure cycle does, however, have the disadvantage that it becomes necessary to control the amount of fuel circulation at low loads. (See Section 3.9). In spite of the added complication of control valves or variable speed pumps, the cycle chosen appeared to offer more advantages and to be more practicable than the critical pressure cycle.

3.5 - Heat Exchanger Design

In an effort to obtain the highest possible thermal efficiency for the overall reactor plant it was originally proposed to use a 4000 psia supercritical steam cycle with this 3500 psia homogeneous reactor. This original steam cycle considered blanket heat exchangers for feedwater heating which would operate at a relatively low temperature (550°F average) and a series of core heat exchangers to deliver main steam at 1150°F. and 4000 psia. No reheating was specified for this original steam cycle. Design specifications for the core exchangers required for the supercritical steam cycle are listed in Table 3.5-1.

TABLE 3.5 - 1

<u>Fluid</u>	<u>Flow</u> lb/hr	<u>Inlet Temp.</u> OF	<u>Outlet Temp.</u> OF	<u>Design Press.</u> Psia.
Steam	1,109,600	592	1150	4000
Soup	2,348,000	1200	750	3500

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Schematic Flow Diagram

ORNL-LR-Diag. 8274
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CORE HEAT = 300 MW
BLANKET HEAT = 45 MW

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W = Flow (#/hr)
T = Temp (°F)
P = Press (psia)

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Figure 3.4-2

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The design of the above heat exchangers was directed toward minimizing holdup and maximizing heat exchange specific power in an effort to obtain reasonably low capital and fuel investment costs. It was decided that high fluid velocities to improve heat transfer at the expense of higher than normal pressure drop would best serve to improve both the specific power and the fuel holdup.

It immediately became obvious that it is difficult to design a high-duty shell-and-tube type heat exchanger with high pressure on both the tube and shell sides, especially at the high operating temperatures involved in this project. The combination of high temperature and pressure requires small diameter tubes and shells with exceptionally thick walls which limits specific power in the heat exchangers. It also became obvious that such heat exchangers would be relatively expensive, since changes in power demand will cause a shifting temperature distribution through the exchangers and require that the entire core exchanger system be designed for high temperature service.

The design of these high pressure heat exchangers was further complicated by the desirability of routing the core soup through the tubes to minimize holdup and the fact that soup flow was appreciably higher than the steam flow rate. Since there was a definite limit on the maximum shell size that could be used under the imposed operating conditions, there was a definite limit to the number of tubes that could be packed into a given heat exchanger shell. In order to limit the soup velocity through the tubes to a reasonable value of 100 fps it was found that approximately 70 heat exchangers in parallel would be required. Baffled shells or a series flow arrangement was also required on the steam side to insure adequate steam velocity. The results of a

preliminary design calculation for the above arrangement using baffled shells is tabulated in table 3.5-2.

TABLE 3.5 - 2

Number of exchangers required	70
Shell ID x Wall x Length	8" x 4.3" x 20'
Soup holdup	950 liters
Soup pressure drop	90 psi
Specific power	0.51 kw/lb HX
Material	100% SS

It should be mentioned that the soup holdup in the above table is based on the heat exchangers alone and does not include the soup which would be held up in the large and complicated header piping required to connect these 70 heat exchangers. The soup pressure drop figure in the table also does not include the orifice pressure drop that would be necessary to insure balanced flow to all exchangers.

In an attempt to reduce the number of heat exchangers required for the supercritical system, an alternative arrangement was considered which divided the core heat exchange system into two parts. One group of exchangers was to handle steam above 1000°F and have the soup routed through the shell side, while the second group was to handle steam below 1000°F and have the core soup in the tubes. The results of a preliminary calculation using this arrangement are tabulated in table 3.5-3.

TABLE 3.5-3

Number of exchangers required	20
Shell ID x Wall x Length	10 @ 13" x 4.5" x 30'
	10 @ 22" x 4.5" x 30'
Soup holdup	19800 liters
Soup pressure drop	70 psi
Specific power	0.46 kw/lb HX
Material	100% SS

Once again it should be noted that holdup and pressure drop presented in table 3.5-3 are for the heat exchangers alone and do not include header holdup or pressure drop, respectively.

Finally, a 2400 psia boiling cycle was considered in an attempt to improve the specific power in the heat exchangers and to reduce material and fabrication costs. This boiling cycle differed from the supercritical cycle in that top temperature was 1100^oF instead of 1150^oF, and one stage of reheat to 1050^oF at 460 psia was included. The efficiency of this boiling cycle would be approximately equal to that of the supercritical cycle since the one stage of reheat would largely make up for the lower steam temperature. The results of the heat exchanger design calculations for this cycle are tabulated in table 3.5-4.

TABLE 3.5-4

Number of exchangers required	2 boilers 5 superheaters 4 reheaters
Soup holdup	2540 liters
Soup pressure drop	180 psi
Specific power	0.78 kw/lb HX
Material	38% CS 52% SS

Based on a comparison of the above tabulations the divided supercritical core exchanger arrangement (Table 3.5-3) was discarded because of low specific power and excessively large fuel holdup.

The 2400 psia boiling cycle was selected for the reactor power system based on a comparison of the heat exchanger requirements for the boiling cycle as presented in table 3.5-4 and the supercritical arrangement outlined in table 3.5-2. The selection was made on the basis of the following:

- (1) Specific power for the heat exchangers is highest in the boiling system.
- (2) Material and fabrication costs should be lower in the boiling system since the entire boiling section with the exception of the soup circuit may be constructed of carbon steel. On a pound basis approximately 35% of the heat exchanger equipment can be carbon steel.
- (3) The fact that only 11 heat exchangers are required for the boiling cycle should result in the least complicated and extensive high temperature soup headering system.
- (4) While pressure drop through the boiling heat exchanger equipment is relatively high, the orificing requirements will be lower than for the supercritical heat exchangers and the overall pressure drops for both systems would very probably be equal in a final design.
- (5) The header piping required to interconnect the reactor core and heat exchangers involves the largest volume of soup holdup in the entire external system. For this reason it is important to keep the amount of external piping required to a

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minimum. It was concluded that the amount of external piping required to interconnect 70 heat exchangers in the supercritical system would be excessive compared to the amount required for the 11 boiling cycle heat exchangers. On this basis it was felt that the boiling system would involve the lowest total fuel holdup.

Heat Exchangers for 2400 psia Boiling Cycle - Line and flow diagrams for the proposed cycle are presented as Figs. 3.4-1 and 3.8-4.

Boiler Design - Design conditions for the boiling section of the cycle are listed below.

Total heat transferred	460×10^6 btu/hr
Steam flow	932,500 lb/hr
Feedwater temperature	589°F
Steam outlet conditions	662°F saturated
Soup flow	1,983,400 lb/hr
Soup inlet conditions	920°F @ 3500 psia
Soup outlet conditions	750°F @ 3500 psia

A horseshoe shaped shell containing U-tubes, refer to Fig. 3.5-1, was chosen as a boiler configuration that would eliminate requirements for a high pressure expansion joint and allow drainability of the soup in the tubes. Headers, tube sheets and tubes in contact with core soup were specified as a stainless steel, similar to type 347. Because of their relatively low operating temperature carbon steel of a type similar to SA 212 Grade B was specified for the boiler shell, drum, and the riser and downcomer piping.

Boiler tubes were designed for 3500 psia and 1000°F. A small tube diameter was desirable to minimize the tube wall thickness required for

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MAIN STEAM GENERATOR
(2 Req'd)

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these design conditions and 3/8" OD x 0.049" wall tubes were ultimately chosen. It was felt that 1/4" OD tubes did not give sufficient flow area per tube.

A decision had to be made regarding the possibility of a scale forming on the steam side or soup side of the boiler tubes. Under the conditions of this design these scales would be the controlling resistance to heat transfer. It was decided to retain conservatism and allow a 1000 btu/hr-ft²-°F scale coefficient on the steam-water side of the tubes and discount any possibility of a scale forming on the soup side of the tubes. Since this scale coefficient is largely controlling in the boiling section the heat transfer was not too dependent upon the boiling coefficient and a conservative value of 2000 btu/hr-ft²-°F was selected for a boiling coefficient at 2400 psia.

Physical properties for the core solution were taken to be those of steam at temperature and pressure.

If single stainless steel tube sheets are specified for each end of the boilers they would have to be approximately 7.5" thick and 38" in diameter. In an effort to reduce the required tube sheet size and thickness it was decided to use 4 smaller tube sheets located in nozzles at each end of the boiler shell. This arrangement reduced tube sheet dimensions to 3-1/3" thick and 15-7/8" in diameter. Locating these tube sheet nozzles on the ends of the boiler shell required that shell diameter be increased from 37" to 52". While this increased the weight and cost of the boilers it was felt that the increased water storage capacity would be beneficial.

Boiler drums were sized to afford space for 20 - 14" conical type cyclones with inclined scrubbers on a 16-1/2" centerline spacing. Six 8" ID riser pipes and three 8" ID downcomer pipes were specified to allow sufficient

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recirculation flow area. Downcomer pipes are connected from the boiler drum to an intermediate header from which eight smaller feeder pipes distribute water evenly along the bottom of the boiler shell.

It was recognized after publication of Figs. 3.5-1 and 3.5-2 in this report that the drainability of the soup from the boilers, superheaters, and reheaters could be improved by locating the entrances to the soup nozzles eccentric and below the centerline of the tube sheet nozzles proper.

Important boiler heat transfer parameters are listed in appendix D.1. Physical properties for the soup were taken as weighted averages over the temperature range involved and hence soup parameters in appendix D.1 are all average values.

An average soup velocity of approximately 75 fps through the boilers was chosen to give a reasonably high film coefficient and yet obtain a fairly low pressure drop of 71 psi. The superheaters and boilers are connected in series on the soup side. The pressure drop was kept low in the boilers in order to retain sufficient pressure drop for the superheaters where a high soup velocity is required for good heat transfer.

Heat transfer calculations indicated that two boilers in parallel, each containing approximately 2500 ft^2 of heat transfer surface were required.

Total soup holdup in the boilers was estimated to be approximately 658 liters. Total boiler weight was estimated to be 102,500 lbs. per boiler. Boiler cost was estimated from the general specifications presented in appendix D.2 and was approximately \$272,000 for two boilers, not including installation.

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Superheater Design - Design requirements for the superheaters are listed below.

Total heat transferred	404.6×10^6 btu/hr
Steam flow	932,500 lbs/hr
Steam inlet conditions	662°F saturated
Steam outlet conditions	1100°F @ 2400 psia
Soup flow	1,983,400 lb/hr
Soup inlet conditions	1200°F @ 3500 psia
Soup outlet conditions	920°F @ 3500 psia

As in the case of the boilers a horseshoe shaped shell containing U-tubes, refer to Fig. 3.5-2, was chosen to eliminate the requirement for a high pressure expansion joint and yet retain pure counter-flow heat exchange and complete soup drainability. Due to the high temperatures involved on both the tube and shell sides, these exchangers would very probably be constructed entirely of a stainless steel, similar to type 347.

Because of the high operating temperatures and pressures in these superheaters, single tube sheets placed at each end of the shells would be excessively thick. To reduce the required tube sheet dimensions it was decided to use two nozzles containing smaller tube sheets at each end of the superheaters. Tube sheets approximately 26-1/2" OD and 8" thick are required for these nozzles. No calculation was made to estimate the thermal stresses which might exist in these stainless steel tube sheets. It is recognized that this nozzle type construction complicates the fabrication of a heat exchanger which is already difficult to build.

Superheater tubes for these design conditions are exceptionally thick-walled. It was decided to keep a minimum tube internal diameter of

SUPERHEATER & REHEATER

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	SUPERHEATER	REHEATER
No. Req'd	5	4
A	7'-0"	7'-0"
B	34"OD x 3.5" wall	22"OD x 1" wall
C	5'-0"	11'-6"
D	2'-2"OD x 3.7" wall	19"OD x 2.5" wall
E	1'-6"	1'-6"
Surface	2170 ft ² (979 - 1/2" OD Tubes)	1925 ft ² (500 - 1/2" OD Tubes)
Load	81.0 MBtu/Hr.	40.0 MBtu/Hr.
Soup Volu.	274.5 Liters	122.0 Liters

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approximately 0.2" and hence 1/2" OD x 0.145" wall tubes were selected for the design. Subsequent heat transfer calculations indicated that these thick tube walls accounted for approximately 30% of the resistance to heat transfer in the superheat section.

A soup velocity of 100 fps was chosen in the superheaters to insure a reasonably high film coefficient. This velocity resulted in a soup pressure drop of approximately 108 psi through the superheaters.

There was some question as to what type of scale might form in the superheaters. As in the case of the boilers it was assumed that no scale would form on the soup side of the tubes but there was some possibility that a scale would form on the steam side. A scale coefficient of 2000 btu/hr-ft²-°F was selected as a reasonable value to account for any steam side scale.

Due to the smaller weight rate of flow for the steam it was necessary to baffle the shell side of the superheaters to insure adequate steam velocity. Half moon type shell baffles on a one foot shell spacing were used to raise maximum steam velocity to 75 fps. Important heat transfer design parameters for the superheaters are listed in appendix D.3.

Five superheaters in parallel were required with a total heat transfer surface of 10,830 ft². Total soup holdup for the superheaters was estimated to be 1370 liters. Total weight for the five units was estimated to be 1370 liters. Total weight for the five units was estimated to be 138, 570 lbs. Superheater cost was estimated from the table of general specifications presented as appendix D.4 and was approximately \$830,000 not including installation.

Reheater Design - Design requirements for the reheaters are listed below.

Total heat transferred	159×10^6 btu/hr
Steam flow	868,620 lb/hr
Steam inlet conditions	710°F @ 460 psia
Steam outlet conditions	1050°F @ 460 psia
Soup flow	364,000 lb/hr
Soup inlet conditions	1200°F @ 3500 psia
Soup outlet conditions	750°F @ 3500 psia

Reheater configuration, refer to Fig. 3.5-2, is similar to that used for the superheaters. Design conditions for these exchangers are somewhat less rigorous than those for the superheaters on the steam side and much thinner shells can be tolerated. Material of construction would still probably be a stainless steel, similar to type 347, due to the temperatures involved. Since the soup system design conditions are identical to those for the superheater a nozzle type construction is used to achieve smaller and thinner tube sheets. As in the case of the superheaters, 1/2" OD x 0.145" wall tubes were specified for the reheaters.

In this design the steam weight flow rate is higher than the soup weight flow rate and no baffling was required on the steam side to achieve the fairly high velocity of 115 fps. A soup velocity through the tubes of 75 fps was chosen to give a reasonably high soup film coefficient with a pressure drop of 110 psi. This pressure drop was not considered to be unreasonable since the combined boiler and superheater pressure drop is 179 psi and hence orificing would be required in the parallel reheat line in any case. See Figure 3.8-4. Some improvement in heat transfer in these reheaters might be obtained by increasing the soup velocity to give somewhat greater pressure

drop and eliminate the need for orificing. The improvement in the overall heat transfer coefficient by this means would probably be limited, since subsequent calculations indicated that the thick walled tubes were strongly controlling. These tube walls accounted for approximately 50% of the heat flow resistance in the reheat section.

Four reheaters, two in parallel and a similar set of two in series with a total heat transfer surface of 7700 ft² were required. Important reheater heat transfer parameters are presented in appendix D.5.

Total reheater soup holdup was estimated to be 512 liters. Total weight for 4 reheaters was estimated to be 34,200 lbs. Reheater cost was estimated from the table of general specifications presented in appendix D.6 and was \$436,000, not including installation.

Blanket Exchangers - Due to the low average blanket temperature the heat produced in the reactor blanket is used for a final stage of feedwater heating. Design requirements for these blanket heat exchangers are listed below.

Total heat transferred	153.8 x 10 ⁶ btu/hr
Feedwater flow	932,500 lb/hr
Feedwater outlet conditions	589°F @ 2500 psia
Feedwater inlet conditions	452°F @ 2500 psia
Blanket soup flow	1,660,000 lb/hr
Soup inlet conditions	621°F @ 3500 psia
Soup outlet conditions	486°F @ 3500 psia

Because of the possibility of corrosion by the blanket soup all sections in contact with the soup would be constructed of a stainless steel, similar to type 347. Carbon steel would be adequate for the shells which operate at a temperature below 600°F. Tube design conditions are not severe and

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1/2" OD x 0.06" wall tubes were selected for these exchangers.

Physical properties for the 500 gram/liter blanket solution were taken from ORNL-CF-54-5-59.

Five blanket feedwater heaters with a total heat transfer surface of 9880 ft² are required in series. Figure 3.5-3 illustrates a possible design for these exchangers which utilizes a labyrinth type expansion joint. These heaters might also be shaped similar to the superheaters and reheaters if it were desired to eliminate this high pressure expansion joint.

There is some question as to whether or not these exchangers actually require expansion joints. The temperature difference between the shell and tubes is practically uniform at 31°F which should result in only about 5500 psi compressive stress in the tubes. Assuming that no expansion joints were present it is very possible that these 5" thick tube sheets could withstand this tendency for the tubes to pull out of the tube sheets. It should be recognized that if no expansion joints were required a simpler design would result.

Heat transfer design parameters for these blanket exchangers are presented in appendix D.7. The weight of 5 of these units was calculated to be approximately 70,000 lbs. The uninstalled cost of five units was estimated to be \$232,000 from the general set of specifications presented as Appendix D.8.

3.6 - Circulating Pumps

3.6.1 - Core System - The core solution leaves the steam generators at 750°F. This relatively high temperature and resulting low density indicate that pump design may be a difficult problem. In the event that a pump could not be designed to handle the core fluid at 750°F, a regenerative type heat exchanger would be required to permit the pump to operate at a lower temperature.

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BLANKET
FEEDWATER HEAT EXCHANGER

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Figure 3.5-3

Needless to say, a heat exchanger of this type is undesirable from a cost viewpoint alone, and every effort to avoid its use becomes worthwhile.

According to Mr. Wayne Ross of ORNL, it is not unreasonable to assume that a canned-rotor type pump could be designed to do the job, provided that corrosion does not preclude a reasonable service life. It appears, however, that compared to the canned-rotor pumps in current use, that the thermal barrier would have to be redesigned. The thermal barrier is the seal which partially separates the fluid, used to cool the motor, from the main fluid stream; the cooling fluid is at a much lower temperature. Furthermore it might be necessary to redesign the motor bearings, because of the characteristics of the core solution and the necessary changes in pump design.

Because of the low density of the fluid being pumped, it is possible that an axial flow type pump would result in a better design than a centrifugal type pump. In general, the axial flow design results in a smaller pump diameter, but greater impeller overhang. Further investigation is required to decide which type design is more desirable. It might be mentioned, however, that the smaller diameter of the axial flow type design appears to offer several advantages because of the relatively high pressure involved.

Another problem attendant to the low fluid density is that of possible overload of the pump motor, if the operating temperature deviates too widely from the design temperature. Figure 2.4-2 is a plot of fluid density versus temperature. In order to prevent pump overload, it is desirable to design away from the temperatures where the slope of the density curve is great, and to operate on the flat portion of the curve. Although this problem is not insurmountable, it is well to be aware that the problem exists.

With regard to pump size and capacities, it can be said, in general, that the larger sizes result in greater efficiencies. Aside from the other considerations of reliability and equipment costs, that of flow control comes into the picture (see Sections 3.4 and 3.9). There are, in general, two ways to control the flow of fuel solution, either through the use of control or throttling valves or by means of pump speed control. Information presently available indicates that the design of valves for this type of service is a difficult task. In the event that pump speed control were necessary, it could be accomplished through the use of a multi-speed motor design or a variable frequency power supply, while retaining the basic canned-rotor type design. The amount of core fluid to be circulated at full load is approximately 30,000 gpm. The full-load pressure drop through the core system is estimated to be 330 psi. Assuming pump net efficiencies of 65%, the amount of energy to be supplied to the pumps is approximately 6.8 MW. The optimum number and size of pumps is a function of, among other things, the type of flow control to be used.

3.6.2 - Blanket System - The circulating pump or pumps for the blanket system will be required to circulate approximately 2000 gpm of $\text{ThO}_2\text{-D}_2\text{O}$ slurry at approximately 486°F. Although no design is available, it is assumed that a canned-rotor type pump could be designed for this service.

3.7 - Piping for Core and Blanket Solutions

For high temperature application and in contact with the blanket solution, stainless steel is required. Type 347 was chosen for its weldability and corrosion resistant qualities. All core fluid piping except the 1200°F outlet piping is to be Schedule 160, and insulated with 2-3" of radiation-stable thermal insulator, thus making the piping essentially isothermal. Hoop stresses at 3,500 psia are acceptable, and piping is sized so that

velocities of the low density core solution are in the neighborhood of 150 ft/sec.

The allowable stress for 347 drops rapidly from 12,500 psi at 1100° to 5,000 psi at 1200°F (ASME Boiler and Pressure Vessel Code, p. 96). For a pressure of 3,500 psi the thickness of a 90% efficient pipe becomes .93 of the inner diameter at 1200°. This obviously is not satisfactory, so it was decided that two courses could be followed: a) the upper temperature could be dropped 50-100°F, thus cutting the plant efficiency, or b) means should be sought to lower the temperature of the pressure-stressed pipe. The latter method was chosen.

For this reason the 1200°F fluid leaving the core is surrounded initially by the 621°F blanket solution. The inner pipe is required to withstand only the 200 psi differential, so that it could be Schedule 10 or 20 pipe. However, at design temperatures there exists a differential of 280°F across a Schedule 20 pipe, leading to a thermal stress of 43,000 psi. This will cause a yielding, but probably not a rupture of the pipe as it is brought up to operating temperature, and a consequent permanent stress when cooled to room temperature. Since the pressure stress is acceptable, it is presumed that the thermal stress will take care of itself. Decreasing the pipe to Schedule 10 would somewhat lower the thermal stress, as would increasing the outer annulus size, so as to decrease the outer film coefficient. The other alternative is to shield the inner pipe with layers of stagnant core fluid. In any case it appears desirable to duct the hot core fluid out through the blanket solution in order to avoid having the 1200°F fluid in contact with the pressure vessel. For the same reason the 750°F fluid entering the core is surrounded by 479° blanket solution. Again the thermal stress in the inner pipe exceeds the allowable.

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Wherever 1200°F core fluid could not be conveniently surrounded by colder blanket fluid, it was decided to leave the pipes uninsulated and put an inner annulus within the pressure pipe to create two stagnant films. Because of the poor radiation and convection coefficient to the atmosphere it is not possible to lower appreciably the upper temperature of the outer pipe. A calculation shows that for a typical section it is possible to lower the temperature approximately 75°F, thus raising the allowable stress from 5,000 to 10,300 psi. This reduces the amount of metal required by a factor of .48, maintaining equal flow areas. However, the outer pipe must withstand in addition to the full core pressure thermal stresses which exceed the allowable. These thermal stresses arise from the radial temperature gradient in the pipe. An alternative to the proposed scheme would be to cool the annulus with water at about 1100°F, and 3,500 psia and insulate the outer pipe so as to minimize the thermal stresses. Thus the outer pipe would be designed for an allowable stress of 12,500 psi. The inner pipe would sustain less than 100°F temperature differential, and would be in no danger.

In either event the piping is complicated and expensive, making it mandatory to minimize this piping when considering the layout. The alternative of decreasing the top temperature and losing in plant efficiency does not appear attractive. Steam at 1150°F and 4,500 psia will be generated by the Philo Plant (Electrical World, June 29, 1953), so that the proposed 1200°F and 3,500 psia core solution does not seem unrealistic from a piping viewpoint.

Stresses in all Schedule 160 piping were calculated by the simple hoop stress formula. Stresses for larger piping were calculated by the

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more exact expression

$$E = \left\{ \frac{1}{\left(\frac{OD}{ID}\right)^2 - 1} \right\} \left\{ \left(\frac{OD}{ID}\right)^2 + 1 \right\}$$

where E was taken as 90% because the pipe must be welded.

Forced convection coefficients were calculated from

$$Nu = .023 Re^{.8} Pr^{.4}$$

and natural convection coefficients from

$$Nu = .13 Gr^{.333} Pr^{.333}$$

where

$$Nu = \frac{hD}{K}$$

$$Re = \frac{GD}{\mu}$$

$$Pr = \frac{c\mu}{K}$$

$$Gr = \frac{g\beta\theta D^3}{\mu^2}$$

Scale coefficients of 2,000 BTU/hr ft² °F were used for both sides, and hydraulic diameter (D_o-D_i) used for annular spaces in Nusselt's and Reynold's numbers. Note that the diameter cancels in the expression for natural convection. Radiation and convection coefficients to the ambient were taken from Marks Handbook, p. 377. All natural convection coefficients are functions of temperature differences, so that iteration is necessary.

Pipe sizes, stresses, temperatures, velocities, etc. are tabulated in Table 3.7-1. The high temperature manifold is shown in Fig. 3.7-1, and the allowable stress of 347 vs. temperature is shown in Fig. 3.7-2.

3.8 - System Layout

A suggested layout is shown in Figs. 3.8-1, 2, and 3. Only main piping systems and heat exchangers are shown for clarity. The heat exchanger room, which is located above ground, is 48' x 64' x 22' high, shielded with 8' walls of Barytes concrete. The overhead is shielded with three 2' sections of concrete slabs. A 40-ton crane travels above the roof and

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UPPER MANIFOLD

DETAILS OF MANIFOLDS
FOR CORE & BLANKET
SOLUTIONS

SCALE 1/2"=1'-0"

FIG 3.7-1

LOWER MANIFOLD

ALL PIPING TO BE TYPE
347 STAINLESS STEEL

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FIG 3.7-1

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ALLOWABLE STRESS FOR VARIOUS STEELS VS TEMPERATURE

FIG 3.7-2

REFERENCE: ASME BOILER &
PRESSURE VESSEL CODE
p 72 & 96

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FIG. 3.7-2

TABLE 3.7-1

PIPING SUMMARY - CORE SOLIN.

ITEM	SIZE	$\sigma_{\text{PRESS.}}$	MAX METAL TEMP	$\sigma_{\text{ALLOW.}}$	Δt ACROSS PIPE	σ_{THERM}	FLOW $\frac{\text{GAL}}{\text{HR}} \times 10^{-6}$	VELOCITY FT/SEC
Upper Manifold Inner Pipe	16"-20	4,920psi	1,100°F	12,500psi	280°F	4,300psi	2.35	131
Upper Manifold Outer Pipe (Blanket Solin)	24"-160	14,400	621°	14,900	~0	~0	.933	15 (Blanket)
Upper Manifold Outer Pipe (Thermally Shielded)	18"x27"	9,260	1125°	10,300	220°	34,000	~0	~0
Inner Pipe to Superheaters	4"-40	~0	<1200°	75,000	~0	~0	.1983	159
Outer Pipe to Superheaters	55"x8.3"	9,260	1125°	10,300	<220°	<34,000	~0	~0
Inner Pipe to Reheaters	3"-40	~0	<1200°	75,000	~0	~0	.093	127
Outer Pipe to Reheaters	45"x6.7"	9,260	1125°	10,300	<220°	<34,000	~0	~0
Superheater to Steam Generator	4"-160	11,300	920°	14,000	~0	~0	.1983	156
	5"-160	12,100	"	"	"	"	.248	124
	10"-160	13,200	"	"	"	"	.992	127
	14"-160	13,900	"	"	"	"	1.983	148
Steam Generators to Pumps	4"-160	11,300	750°	14,700	~0	~0	.248	112
	8"-160	13,200	"	"	"	"	.992	114
Reheaters to Pumps	2½"-160	8,400	750°	14,700	~0	~0	.093	111
	3"-160	10,500	"	"	"	"	.185	143
	5"-160	12,100	"	"	"	"	.370	106
Pump to Reactor	12"-160	13,500	750°	14,700	~0	~0	2.35	122
Reactor Inlet Inner Pipe	12"-30	3,660	<750°	714,700	~130°	~20,000	2.35	86
Reactor Inlet Outer Pipe (Blanket Solin)	18"-160	14,200	479°	15,200	~0	~0	.933	20 (Blanket)

lifts these slabs for access to the heat exchangers, reactor vessel, pumps, etc. Three reactor compartments share two common walls. The turbo-generator rooms are located at one end of the building. Only the steam generators, superheaters, reheaters, and blanket-heated feed water heaters are enclosed in the shielded section.

Three major criteria were kept in mind in laying out the heat exchanger room: thermal expansion of the piping, minimizing all 1200° core fluid piping, and drainability. To some extent these criteria conflicted, so that the proposed layout is essentially a compromise. Adequate flexibility of the piping is achieved, but at the same time all core fluid piping is held to a reasonable length. By pitching the piping slightly the system is drainable to the core and thence to the dump tanks.

In general steam piping was considered to be of secondary importance to the core and blanket piping. However, since the throttle steam is at 2500 psia and 1100°F, it is desirable that this piping should not be unnecessarily long.

The reactor pressure vessel is located in a concrete-lined hole 16' x 16' x 25' deep. A removable concrete shield overhead serves to protect the equipment above from full core neutron radiation. Pressurizers for both the core and blanket fluids are located beneath the pressure vessel. Dump tanks for the core and blanket are in three water-filled compartments located adjacent to the compartment for the reactor core. For a more detailed description of the dump tanks and auxiliary systems see Section 3.10.

Core fluid pressure drops are tabulated in Table 3.8-1, and core fluid weights are tabulated in table 3.8-2.

TABLE 3.8-1

WEIGHT SUMMARY FOR CORE SOLUTION

Core	343 lbs
Heat Exchangers	448 lbs
Piping	<u>1049 lbs</u>
	1840 lbs

Total-to-core weight ratio = 5.36

Piping accounts for 57% of the total holdup

TABLE 3.8-2

SUMMARY OF PRESSURE DROPS IN CORE PIPING SYSTEM

Reactor	10 psi
Piping to superheater	15
Superheater	108
Piping to steam generator	55
Steam generator	71
Piping to reactor	<u>69</u>
	330 psi

43% of total pressure drop in piping

Pumping power = 6.8 MW @ 65% pump efficiency
(or 2% of heat output required to pump the
core solution)

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ARRANGEMENT - PLAN VIEW
THROUGH HEAT EXCHANGERS
SCALE $\frac{1}{8}" = 1'-0"$

FIG. 3.8-1

FIG. 3.8-1 UNCLASSIFIED

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ARRANGEMENT - PLAN VIEW
ABOVE HEAT EXCHANGERS

SCALE $\frac{1}{8"} = 1'0"$

FIG 3.8-2

FIG. 3.8-2 UNCLASSIFIED

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GANTRY CRANE OVERHEAD

REMOVABLE CONCRETE SLABS
FOR ACCESS TO EQUIPMENT

TURBO-GENERATOR ROOM

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22'-0"

STEAM DRUM

SUPER-
HEATER

STEAM GEN.

REHEATER

REACTOR
PRESSURE
VESSEL

DUMP TANK
ROOM

EXPANSION
PLENUM

DUMP TANK ROOM

TO BLANKET
DUMP TANKS

CORE PRESSURIZER BLANKET PRESSURIZER

FIG 3.8-3

ARRANGEMENT - ELEVATION
THROUGH REACTOR
SCALE 1/8"=1'-0"

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3.9 - Control

It is felt that this reactor, the HTHR, with regard to nuclear stability and control, should behave in a manner similar to the lower temperature aqueous homogeneous reactors. The temperature coefficient of reactivity should be sufficiently negative to result in stable operation without external control.

There is, however, one major difference between the reactor system, in question here, and the lower temperature homogeneous reactor; this difference, which results in the necessity of flow control of the fuel solution, stems directly from the fact that the average temperature of the fuel solution is above the critical temperature. When load demand falls off in an ordinary homogeneous reactor system, the pressure of the secondary coolant (steam to the turbine) is permitted to increase. Allowing the pressure of the secondary coolant to increase results in a lower temperature difference between the fuel solution and the coolant, which in turn leads to less heat transfer, and permits the reactor output to match the load demand without decreasing the water level in the heat exchanger. Thus, the water on the secondary side of the heat exchanger is not all boiled away. It should be noted also, that in the case described, the shell of the heat exchanger must only be able to withstand the saturation temperature of the secondary coolant.

In the case under consideration here, however, the average temperature of the fuel solution is above the critical temperature. Obviously, then, the degree to which the temperature difference, between the fuel solution and the secondary coolant, can be reduced, by raising the pressure of the secondary coolant, is definitely limited. Hence, at low load demand, it

is necessary to control (decrease) the flow of fuel solution in order to prevent all the water on the secondary side of the exchanger from being boiled away. Flow control permits the use of a lower design temperature in the shell of the heat exchanger than would otherwise be possible.

The flow of fuel solution would be controlled so as to maintain the desired pressure in the secondary coolant system. Water level, in the steam generator, would be maintained by means of standard boiler-feed pump control. In order to maintain the desired temperature (average) of the fuel solution, the fuel concentration would be controlled. The latter is not a "fast acting" control and could not be relied upon for instantaneous response.

3.10 - Core and Blanket Pressurizers

Pressurizers presently designed for the HRT, work on the principle that the maintenance of an interface between the core (or blanket) fluid, which is a liquid, and the vapor space above it will serve to act as a surge volume, thus damping out pressure surges as well as to provide a means of controlling operating pressure.

The reactor designed in this report will operate at a pressure in the supercritical region thus eliminating the presence of any interface between liquid and vapor. This essentially means that the core is its own pressurizer at operating conditions. A pressurizer is necessary mainly for startup to bring the core (and blanket) up to operating temperature and pressure, although it may exhibit some small effect during operation through use of a spray to reduce pressure and electric heat to increase pressure.

The pressurizers will be located below the reactor (see Fig. 3.10-1) and startup will consist of injecting a measured amount of light water into the core pressurizer and heavy water into the blanket pressurizer and heating

CORE AND BLANKET DUMPING SYSTEMS

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FIGURE 3.10-1

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electrically along the legs connecting the pressurizers to the core and blanket.

When desired temperature and pressure are reached, the fertile material may be pumped into the blanket and blanket circulation started. The fuel may then be added to the core by means of a pulsafedder pump until criticality is reached.

3.11 - Core and Blanket Dump System

Dump systems for both core and blanket fluids were designed to perform several functions:

A. To reduce K below 1.00 in case of a sudden increase in reactivity which the normal temperature coefficient could not handle. This reduction in K (about 2%) would be produced by the sudden expansion of some of the core and blanket fluids into separate surge tanks, reducing the density and making the core subcritical.

B. In case of a leak, it would be necessary to dump the core and blanket and to store in a safe place, the whole operation taking about five minutes. Cooling is provided by spiral tubes connected between the surge tanks and oversafe storage vessels. The whole dump system is immersed in a pool of water. Orifices between the surge tank and cooling tubes would regulate the flow so that the total dumping time is five minutes. It is felt that this is a reasonable amount of time to cool the fluids with the limited amount of cooling surface available. The rate of heat transfer to the pool per tube may be decreased by either allowing for a longer dumping time or by increasing the number of cooling tubes since neither appears to be a critical item.

C. It will be necessary at times to shut down the plant for routine maintenance in which event the dump system will be used to cool down the

core and blanket fluids so that they may be stored as liquid in the tanks provided.

The large heat capacity of the pool of water serves to cool the soup and fertile materials to a temperature where they may be stored conveniently and kept cool with a minimum of complexity.

It can be seen from Fig. 3.10-1 that only one valve is necessary between the core and surge tanks, and likewise between the blanket and its surge tank. Thus a fair amount of dependability is assured. It must be realized, however, that during a dump, the pressure of core and blanket must be equalized within limits at all times. At present, the best method of controlling the pressure difference is through the use of remotely operated valves which are actuated by a pressure balancing device. The balancing device electronically opens and closes the valves so that a pressure difference of no more than 40 psi is realized.

Thermal shock in the surge tanks and connecting cooling tubes is a serious problem. To minimize this thermal shock, the surge tanks are jacketed and insulation is placed on the outer surface of the jackets. Low pressure steam is bled through the jackets at temperatures close to inlet temperatures of the core and blanket fluids. The initial temperature rise in the cooling tubes is not as serious since the tube ends, near the surge tank, are warmed by conduction.

Cooling water may be circulated through the pool and dumped along with the condenser water. In case of a rupture in the dump system, the water could no longer be dumped and boiling in the pool would commence. The vapors would then be drawn up a stack to the point where fission gases are normally absorbed.

Boron pebbles or plates may be stacked at the bottom of the pool to solve the problem of criticality in case of the settling of fissionable materials to the bottom of the pool after a rupture of either tank or cooling tubes. See Table 3.11-1 for specifications.

TABLE 3.11-1

DUMPING SYSTEM SPECIFICATIONS

ITEM	DESCRIPTION	SIZE	MATERIAL
A	Core surge tank (1)	12" I.D.-9' long	Stainless Steel
B	Blanket surge tank (1)	6" I.D.-5.75' long	Stainless Steel
C	Core fluid surge tanks (12)	5" I.D.-20' long	Carbon Steel
D	Blanket fluid storage tanks (5)	2'-2" I.D.-20' long	Carbon Steel
E	Plenum from C (2)		Stainless Steel
F	Plenum from B (1)		Stainless Steel
G	Plenum from D (1)		Stainless Steel
H	Core pressurizer (1)	7" I.D.-10' long	Stainless Steel
I	Blanket pressurizer (1)	7" I.D.-10' long	Stainless Steel
J	Core fluid cooling coils (12)	3/8" tubing-30' long	Stainless Steel
K	Blanket fluid cooling coils (5)	3/8" tubing-30' long	Stainless Steel
L	Core pulsafeeder pump (2)		
M	Blanket pulsafeeder pump (1)		
N	Off-gas from core storage (2)		
P	Off-gas from blanket storage (1)		
R	Steam jacket for core surge tank (2)		Carbon Steel
S	Steam jacket for blanket surge tank (1)		Carbon Steel

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3.12 - Let-Down System

The let-down system (Fig. 3.12-1) is used to remove non-condensable vapors from the main soup stream. About 1/1000 of the main stream is continuously bled from the discharge of the main circulating pump and passed through an orifice dropping the pressure from 3800 psi to 3150 psi which is below the critical pressure thus permitting two phase flow to allow removal of vapors. It is then passed through the shell of a singly baffled surface condenser using 600°F steam as coolant in 16 U-bent tubes. The accumulation of vapors at the top of the condenser are sent through a small vent condenser to remove any residual condensable vapors before flowing to the off-gas system.

Since the main circulating pump has a head of about 300 psi, a small capacity pump supplying a head of 360 psi is inserted in the system to add the necessary head to permit flow of the condensed soup back to the suction of the main pump. See Table 3.12-1 for specifications.

TABLE 3.12-1

LET-DOWN SYSTEM SPECIFICATIONS

Soup Condenser:

Overall length.....9'
Pressure.....3150 psi
Temperature.....710°F
Tubes (U-bent, Stainless Steel).....16 required
 length.....17'
 O.D.0.375"
 I.D.0.277"
Shell (Stainless steel)
 O.D.7.72"
 I.D.6"
 length.....8.25'
Tube sheet (Stainless Steel)
 O.D.7.72"
 thickness.....2.8"

Vent Condenser:

Overall length.....2.5'
Pressure.....3150 psi
Temperature.....700°F
Tubes (Straight through, Stainless Steel).....8 required
 average length.....1.75'
 O.D.0.375"
 I.D.0.277"
Shell (Stainless Steel)
 O.D.6.5"
 I.D.5.0"
 length.....1.5'
Tube Sheet (Stainless Steel)
 O.D.6.5"
 thickness.....2.6"

Soup flow rate2460 lbs/hr.
Steam flow rate4800 lbs/hr.
Heat transferred.....515000 BTU/hr.

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FIGURE 3.12-1

LET-DOWN SYSTEM

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CHAPTER 4 - COST ESTIMATION

4.1 - Summary

	2 Reactor Station		3 Reactor Station	
Plant Capacity (MW-Elec. Net)	290		435	
Plant Capacity Factor	80%		80%	
Elec. Generation (KWH/YR.)	2.032 x 10 ⁹		3.048 x 10 ⁹	
	\$/Yr.	Mills/KWH	\$/Yr.	Mills/KWH
Plant Fixed Charges	8,192,000	4.03	12,094,000	3.97
Fuel Cost	3,222,000	1.59	3,977,000	1.27
Operation	388,000	0.19	581,000	0.19
Maintenance	819,000	0.40	1,209,000	0.40
TOTAL	12,621,000	6.21	17,761,000	5.83

4.2 - Capital Cost of Plant

Table 4.2-1 shows a breakdown of the plant investment, exclusive of the chemical processing facilities. Two cases are shown: a two reactor station and a three reactor station.

In preparing the breakdown, a report on the TBR by R. B. Korsmeyer was used as a guide. Wherever possible, the costs of individual pieces of equipment were calculated separately. For example, the costs of the heat exchangers, pumps, core and pressure vessels, thermal shield, and piping were estimated in accordance with the methods suggested in "Cost Estimating Information, TBR Memorandum No. 4" (CF 54-10-11) by M. I. Lundin.

In the cases where the lack of a detailed design precluded accurate costing, the costs were derived by adjusting, in an appropriate manner, the costs shown by Mr. Korsmeyer for the TBR.

It will be noted that the costs shown for plant investment do not include investment for chemical processing facilities.

The TBR report shows an investment cost, including chemical processing facilities, of \$201/KW for a three reactor (300 MW elec.) plant. Allowing \$23/KW for chemical processing facilities, the latter figure is reduced to \$178/KW, which compares to the HTHR costs of \$188/KW for a two reactor (290 MW elec.) plant or \$185/KW for a three reactor (435 MW elec.) plant. The need for heavier pressure equipment and, in some cases, more elegant materials in the high temperature plant tends to increase the cost. This trend is partially compensated, however, by the smaller size, on an electrical kilowatt basis, of some of the major components. The latter effect results from the fact that due to the greater thermal efficiency of the cycle, less heat need be transferred per KWH of electrical output.

TABLE 4.2-1

CAPITAL COST OF HTHR

A. Reactor Plant

1. Reactor Proper

High Pressure System		
Core Vessel	81,000	
Core Pressurizer	50,000	
Heat Exchangers	1,682,000	
Circ. Pump (Core)	750,000	
Circ. Pump (Blanket)	35,000	
Pressure Vessel	244,000	
Thermal Shield	40,000	
Blanket Pressurizer	40,000	
SUB-TOTAL		\$2,922,000
Low Pressure System		
Core	300,000	
Blanket	300,000	
SUB-TOTAL		\$ 600,000
Common System		
(ventilation, F.P. removal, etc.)	100,000	\$ 100,000
Sampling	100,000	\$ 100,000

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TABLE 4.2-1 (cont.)

Piping, Valves, Etc.		
Piping	750,000	
Valves	75,000	
Insulation	40,000	
SUB-TOTAL		\$865,000
Installation of Reactor, Pumps		
Heat Exchangers, etc.		
(25% on vessel and shield;		
40% on rest)	1,434,000	\$1,434,000
Instruments and Controls	300,000	300,000
SUB-TOTAL		\$6,321,000
2. Reactor Cell		
Cell Vessel	350,000	
Excavation, Structural, Concrete	575,000	
SUB-TOTAL	925,000	\$925,000
SUB-TOTAL - COST PER REACTOR		\$7,246,000
	2 Reactor Station (290 MW)	3 Reactor Station (435 MW)
Cost of Reactors	\$14,492,000	\$21,738,000
3. Reactor Station		
Crane	250,000	250,000
Recirc, water system		
and towers	160,000	200,000
Common soup and blanket		
facilities	200,000	250,000
4. Station General		
Land Rights and Improvements	900,000	1,000,000
Maintenance	900,000	1,000,000
Basic Cost	16,902,000	24,438,000
Contingencies (25%)	4,226,000	6,109,000
SUB-TOTAL	21,128,000	30,547,000
Engineering and Design (10%)	2,113,000	3,055,000
Fees (3%)	634,000	916,000
TOTAL REACTOR PLANT COST	23,875,000	34,518,000
B. Turbogenerator Plant (\$106/KW)	30,740,000	46,110,000
TOTAL STATION INVESTMENT	<u>\$54,615,000</u>	<u>\$80,628,000</u>
COST PER KW	<u>\$188</u>	<u>\$185</u>

4.3 - Fuel Costs

4.3.1 - Summary - Shown in Table 4.3-1 is a summary of fuel component costs for "on site" and central-plant processing. A total fuel cost of 1.59 mills/kw-hr using "on site" processing was obtained and used in calculating total power costs for the two-reactor HTHR plant. If a central chemical plant is used for processing the reactor materials, a low fuel cost of 0.67 mill/kw-hr results. The HTHR is similar enough to the TBR-1 that fuel costs can be compared. TBR-1 fuel costs are also listed in Table 4.3-1. To make this comparison the same cost equation and material charges were used for the HTHR and TBR-1. This information and the basic design data for both reactor systems are listed in Table 4.3-1. The TBR-1 plant has a similar "on site" chemical plant, but uses a hydraulic solids separation system which the HTHR does not have.

Reactor fuel costs are subdivided into the following components:

- (1) cost of chemical processing and D_2O recovery, in which comparison is made between processing costs for a small capacity "on site" plant and a large capacity centrally-located plant,
- (2) cost of uranium and thorium consumed,
- (3) inventory charges and losses of uranium and thorium in the reactor and chemical plant,
- (4) inventory charges and losses of D_2O in the reactor and chemical plant.

4.3.2 - Chemical Processing Costs - In estimating Thorex processing, two cases were studied for comparison purposes. In the first case, the Thorex plant is located adjacent to the 2-reactor power station. The plant has a capacity of 142 Kg of thorium per day and purchases steam, water, electricity, and probably high-pressure air from the reactor station. In addition, other

economics will result from sharing such facilities as the stack for off-gas dispersal, instrument repair shop, maintenance personnel, guards, etc.

The D_2O is recovered from the blanket slurry in the Thorex plant.

The following cost equation was determined by E. D. Arnold (CF-55-3-40) to hold for a Thorex processing capacity range of 100 to 600 Kg. thorium per day:

$$\text{Daily Cost} = \$5250 + \$3.50W + \$0.54U + \$0.35D$$

where: W = thorium throughput, Kg/day
 U = total U^{233} throughput, gm/day
 D = D_2O throughput, liters/day

This equation was used to give a daily cost of \$6783 for processing the two reactors. The daily cost may be somewhat less than this due to the above mentioned economics of the Thorex plant which were not taken into account.

In the second case studied, a centrally-located Thorex plant serves a number of similar 2-reactor power stations. The cost of chemical processing is highly dependent upon the total capacity of the Thorex plant since capital and operating costs do not vary appreciably with plant capacity. A detailed cost estimate has been made by A. R. Irvine for a plant of 3000 Kg. per day capacity (Ferguson & Sanders, CF-53-6-173). Such a plant could process about 20 units like the HTHR power station at a cost of \$5.66 per Kg. of thorium. This cost estimate was based on a plant processing thorium which contains one gram of U^{233} per kilogram and which has been cooled for 30 days. Although the Thorex feed for the HTHR plant contains about 15 gm U^{233} per Kg. thorium, it is believed that the processing costs are not sensitive to the amount of U^{233} in the feed (private communication with A. R. Irvine). The central plant was also designed to include several pieces of equipment that

could be eliminated in a homogeneous reactor processing plant with a possible saving in cost.

The D_2O is recovered at the reactor station site before cooling and subsequent shipping of the blanket and core materials to the Thorex plant. The Vitro Corporation has estimated the cost of D_2O removal at \$0.70 per lb. for small installations (KLX-1611). A shipping charge of \$4.00 per Kg. of thorium was chosen based on figures used by the Nuclear Power Group (NPG-112, p.67). Using these values, the daily fuel processing cost for the HTHR 2-reactor station is found to be \$1704.

4.3.3 - Uranium and Thorium Makeup Costs - Since the HTHR has a breeding ratio greater than unity, the net amount of U^{233} gained in the HTHR with a breeding ratio of 1.003 is 1.82 grams per day. However, approximately the same amount of U^{233} is lost in chemical processing and the power plant is just self-sufficient in fuel.

In calculating thorium burnup, it was assumed that 5% of the thorium yields higher isotopes which are removed in chemical processing. Based on a value of \$25 per Kg. (CF-54-4-170, p.2), the daily cost of thorium consumed in the 2-reactor plant is found to be \$16.

4.3.4 - Inventory and Losses of U^{233} and Thorium - A 4% charge for non-depreciating items was assumed in calculating the cost of U^{233} inventory. A cooling time of 90 days for the core material and 65 days for the blanket material was chosen. These cooling periods appear to be sufficient to allow for shipping, processing, and Thorex plant shutdowns. Using these values, a daily cost of \$1696 for U^{233} inventory results. For "on site" processing, 10 days allowed for shipping can be deducted to give a daily cost of \$1657.

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The daily cost of thorium inventory is \$71 for central plant processing and \$68 for "on site" processing.

Assuming processing losses of 0.1% the daily loss of U^{233} are 1.9 grams. Thorium losses for the HTHR are small and are neglected in this study.

4.3.5 - Inventory and Losses of D_2O - Using a 4% annual charge for non-depreciating items, a 5% annual loss of inventory, and a value of D_2O of \$28/lb., the daily cost of inventory and loss of D_2O is \$304.

4.3.6 - Three-Reactor Station - The daily capital and operating costs of the chemical plant were assumed to be \$5250 for both a two and a three-reactor station. All other fuel component costs will vary directly with the number of reactors. The fuel costs for an "on site" chemical plant serving a three-reactor station is found to be \$10,620 per day or 1.27 mills per kw-hr.

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TABLE 4.3-1

FUEL COMPONENT COSTS

	HTHR "On Site" Plant (2-reactors)		HTHR Central Plant (2-reactors)		TBR-1 (3-reactors)	
	\$/day	Mill/kw-hr	\$/day	Mill/kw-hr	\$/day	Mill/kw-hr
Chemical Plant, Capital & Operating	5250	0.95			5250	0.91
Thorium Processing	497	0.09	804	0.14	1144	0.20
Uranium Processing	965	0.17			901	0.16
D ₂ O Recovery	71	0.01	346	0.06	244	0.04
Shipping	-	-	568	0.10	-	-
Total Chemical Processing Costs	6783	1.22	1704	0.30	7539	1.31
U ²³³ Credit	(-29)	(-0.01)	(-29)	(-0.01)	(-2885)	(-0.50)
U ²³³ Losses	28	0.01	28	0.01	27	0.00
U ²³³ Inventory	1657	0.30	1696	0.31	709	0.12
Thorium Makeup	16	0.00	16	0.00	35	0.01
Thorium Inventory	68	0.01	71	0.01	176	0.03
D ₂ O Inventory	135	0.03	135	0.02	518	0.09
D ₂ O Losses	169	0.03	169	0.03	647	0.11
Hydroclone Separation	-	-	-	-	271	0.05
Total Fuel Cost	8827	1.59	3790	0.67	7042	1.22

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TABLE 4.3-2

BASIC DATA AND ASSUMPTIONS FOR FUEL COST ESTIMATION

Value of fuel grade U ²³³ (Replacement or sale)	\$16/gm	
Value of thorium	\$25/Kg.	
Value of D ₂ O	\$28/lb	
U ²³³ processing losses of 0.1% of amount processed		
D ₂ O losses of 5% of inventory per year		
D ₂ O annual inventory charge of 4%		
U ²³³ annual inventory charge of 4%		
Thorium annual inventory charge of 4%		
Daily chemical processing cost (on site plant)		
= \$5250 + \$3.50W + \$0.54U + \$0.35D		
	HTHR 2-reactor plant	TBR 3-reactor plant
W Thorium throughput (Kg/day)	142	327
U Total U ²³³ throughput (gm/day)	1907	1669
D D ₂ O recovered (liters/day)	205	696
U ²³³ gained in breeding (gm/day)	1.82	180.3
Thorium makeup (Kg/day)	0.64	1.39
U ²³³ processing losses (gm/day)	1.9	1.7
D ₂ O losses (lb/day)	6.1	23.1
Thorex processing cycle of core loop (days)	406	184
Thorex processing cycle of Blanket loop (days)	90	130
U ²³³ inventory for reactor and on site chem. plant (Kg)	945	404
Thorium inventory for reactor and on site chem. plant (Kg)	24,900	64,100
D ₂ O inventory for reactor and on site chem. plant (Kg)	44,500	168,000
Hydroclone separation (\$/day)	-	276

4.4 - Operation and Maintenance Cost

It should be noted that the costs shown in Section 4.1 for operation and maintenance do not include the costs for the operation and maintenance of the chemical processing facilities. The latter costs are included under "Fuel Cost".

The methods used for estimating operation and maintenance costs are those used in the TBR study.

Accordingly, the yearly operation cost was estimated to be:

$\$1.25 \times \text{Plant Gross Capacity (Electrical Kilowatts)}$.

Yearly maintenance costs were estimated to be:

$1.5\% \times \text{Plant Capital Investment}$

	2 Reactor Station	3 Reactor Station
Net Capacity (MW)	290	435
Gross Capacity (MW)	310	465
Operation (\$/Yr.)	\$388,000	\$581,000
Maintenance (\$/Yr.)	\$819,000	\$1,209,000

APPENDIX A

PRESSURE VESSEL AND THERMAL SHIELD

A.1 - Gamma Currents Entering Thermal Shield

The following sources of gammas were assumed in the core: chromium capture yields .69 -9.72 mev and .18 -6.0 mev gammas (Nucleonics, May 1955, p. 50); uranium 233 fission yields 5 -.10 mev prompt and 2 -2.5 mev delayed gammas, and capture yields 2 -2.25 mev gammas (CF-54-10-76, p. 38). The total fission density can be calculated readily by assuming a power density of 150 watts/cc and using the conversion factor 3.38×10^{10} fissions/watt sec. Knowing the microscopic cross section at the 550°F neutron temperature and assuming that the molecular concentration of chromium is twice that of the uranium, the average source strength of all core gammas can be calculated. It was assumed that the thermal flux and hence the source strength was independent of the radius. This assumption somewhat simplifies the calculations and leads to no serious error in this case, since self-attenuation in the core is small because of the extremely low densities. In order to estimate the core attenuation it was assumed that the core composition consisted of D₂O at 1050°F and 3500 psia ($\rho = .0807$ g/cc) with 25 g/liter of uranium. All mass absorption coefficients were divided by 1.2 in order to approximate the buildup factor. This greatly simplifies the calculations, and appears to be an acceptable approximation, especially since all values for μr are small.

Chapman (CF-54-10-76, Appendix A) develops the expression for the current at the surface of a sphere:

$$J_c = \frac{1}{4R_c^2} \int_0^{R_c} S(r) \left\{ (R+r) F_1 [\mu(R-r)] - (R-r) F_1 [\mu(R+r)] + \frac{1}{\mu} \left(e^{-\mu(R-r)} - e^{-\mu(R+r)} \right) \right\} r dr$$

Note that this expression gives the current J_C , in gammas/cm² sec, but does not specify the angular distribution. Two numerical calculations were made for representative values of μ with $R = 80$ cm and $S \neq f(r)$. The self attenuation as a function of μ is plotted in Fig. A.1-1. Note that since the values of μR are so low, the maximum core self attenuation (1 mev gammas) is only 33%. This justifies the assumption that knowing the exact flux pattern in the core is not too important for this case.

In order to evaluate the attenuation of the core gammas in the blanket, an angular distribution of gammas emerging from the core was assumed;
 $J(\xi) = J_R \cos^2 \xi$, or $P(\xi) = 3 \cos^2 \xi \sin \xi d\xi$. In order to obtain a quick analytic solution, the blanket was treated as a slab, but the current at the blanket edge was modified by the ratio of the radii squared. Note that when these calculations were made it was assumed that the thickness of the blanket was 38 cm. This thickness was later increased in the final proposal.

$$\begin{aligned}
 J_B &= J_C \left(\frac{R_C}{R_B} \right)^2 \int_{\xi=0}^{\pi/2} P(\xi) d\xi e^{-\mu r} \\
 &= J_C \left(\frac{R_C}{R_B} \right)^2 \int_{\xi=0}^{\pi/2} 3 \cos^2 \xi \sin \xi d\xi e^{-\mu r}
 \end{aligned}$$

$$T = r \cos \xi \quad \text{and} \quad dT = 0 = \cos \xi dr - r \sin \xi d\xi$$

$$\begin{aligned}
 J_B &= 3 J_C \left(\frac{R_C}{R_B} \right)^2 \int_{r=T}^{\infty} \frac{T^3}{r^4} e^{-\mu r} dr \\
 &= 3 J_C \left(\frac{R_C}{R_B} \right)^2 E_4(\mu T)
 \end{aligned}$$

$$\text{where } E_4(\mu T) = \int_{\lambda=1}^{\infty} e^{-(\mu T)\lambda} \lambda^{-4} d\lambda$$

In order to evaluate the blanket attenuation factors it was assumed that the composition consisted of D₂O at 550°F and 3500 psia, with 500 g/liter of thorium ($\rho_{\text{ThO}_2} = 0.69 \text{ g/cc}$). Buildup was again accounted for by dividing the mass absorption coefficient by 1.2.

In order to estimate the gamma current at the blanket edge resulting from gamma generation in the blanket, several assumptions had to be made. See Fig. A.1-2 for the estimated shape of the neutron flux in the blanket. Blanket power was assumed to be 10% of that in the core. This was later increased to 15%, but since the major portion of the gamma currents entering the thermal shield is due to core gammas, no serious error is involved. A uranium 233 concentration of 3 g/liter of thorium was assumed, with a thorium concentration of 500 g/liter. It was assumed that thermal neutron capture in thorium results in 3 -3.0 mev gammas.

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FRACTION OF CORE GAMMAS
ESCAPING FROM 80 GM CORE
VS μ , MASS ABSORPTION COEFF.

FIG A.1-1

FRACTION ESCAPING =

$$\frac{1}{4R^2} \int_0^R \{ (R+r)F[\mu(R-r)] - (R-r)F[\mu(R+r)] + \frac{1}{\mu} [e^{-\mu(R-r)} - e^{-\mu(R+r)}] \} r dr$$

$\frac{R}{3}$

FRACTION OF CORE GAMMAS ESCAPING

ASSUMPTION: SOURCES
INDEPENDENT OF CORE
RADIUS

0.002

0.004

0.006

029 109

μ - MASS ABSORPTION
COEFF (INCLUDING BUILDUP
CORRECTION) - GM⁻¹

FIG. A.1-1

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ASSUMED THERMAL NEUTRON
FLUX IN BLANKET

FIG A.1-2

FIG A.1-2

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In order to obtain the current at the thermal shield due to blanket gammas, the previously mentioned integral equation derived by Chapman was used, letting $S(r) = 0$ for $r < R_C$ (80 cm), and allowing the source strength to vary as the neutron flux for the case of all but the delayed neutrons from fission. This source was assumed to be independent of radius, and no account was made for losses of delayed neutrons in circulation. Note that in making the integration it is necessary to assume that the absorption coefficients in the core and blanket are the same. This is decidedly not true for this case, since the densities are so different. Implicit in the integration is an underestimation of the gamma current, since the attenuation of the gammas passing through the core is less than predicted. Since the major portion of the current does not pass through the core, and since the core gammas overshadow the blanket gammas, it is felt that this approximation does not lead to a serious error. See Table A.1-1 for a summary of all gamma production in the core and blanket, with currents at the blanket edge.

A.2 - Thermal Neutron Current Entering the Thermal Shield

ORACLE calculations for $\bar{C} = 3500$ and 5000 cm^2 fix the thermal leakage as 11.1% of all uranium 233 absorptions for the case of the small (8.42') pressure vessel. On this basis a thermal leakage of $8.50 \times 10^{12} \text{ neut/cm}^2 \text{ sec}$ was calculated at the blanket edge. By going to the larger pressure vessel the thermal leakage is reduced to 4.64%, and the current at the thermal shield is reduced by a factor of .28 to 2.40×10^{12} . The larger current was used in all of the calculations, since at the time the design was far from firm. Neutron flux in the iron was assumed to obey the following relation:

$$\phi_n(R_b + r) = \frac{J_B}{KD} e^{-Kr}$$

TABLE A.1-1

SUMMARY OF GAMMA CURRENTS

Core Gammas ($R_C = 80^{\text{cm}}$, $R_b^* = 118^{\text{cm}}$)

<u>E Mev</u>	<u>Source</u>	<u>Strength, γ/cc sec</u>	<u>μ_c</u>	<u>Core Atten.</u>	<u>J₈₀</u>	<u>μ_b</u>	<u>Blank. Atten.</u>	<u>J₁₁₈</u>
9.72	Cr Cap	3.09×10^{10}	.0025	.84	69.1×10^{10}	.036	.0743	5.1×10^{10}
6.0	Cr Cap	1.01×10^{10}	.0028	.82	22.0×10^{10}	.038	.0681	1.50×10^{10}
2.5	Delay & Cap	11.3×10^{12}	.0038	.77	231×10^{12}	.047	.0445	10.3×10^{12}
1.0	Prompt	25.2×10^{12}	.0065	.67	448×10^{12}	.081	.0094	4.2×10^{12}

Blanket Gammas

<u>E Mev</u>	<u>Source</u>	<u>Avg. Strength</u>	<u>μ b</u>	<u>Blank. Atten.</u>	<u>J₁₁₈</u>
3.0	02 Cap	2.02×10^{12}	.044	.118	6.45×10^{12}
2.5	23 Delay & Cap	$.509 \times 10^{12}$	.047	.195	2.69×10^{12}
1.0	23 Prompt	1.15×10^{12}	.081	.0535	1.67×10^{12}

Total Gamma Currents at $R_0 = 118^{cm}$

<u>E Mev</u>	<u>Current, 0/cm²sec</u>	<u>μ_{Fe}</u>	<u>$(\mu - \sigma)_{Fe}$</u>
9.72	5.1×10^{10}	.197	.20
6.0	1.5×10^{10}	.20	.184
3.0	6.5×10^{12}	.23	.165
2.5	13×10^{12}	.24	.163
1.0	5.9×10^{12}	.39	.20

* Note: The proposed blanket has been increased so that $R_b = 142$ cm.

[illegible]

A.3 - Fast Neutron Current Entering the Thermal Shield

Since the heating resulting from the inelastic scatter of fast neutrons is small in comparison with that due to gamma attenuation and thermal neutron capture gammas, no attempt was made to estimate this source.

A.4 - Estimation of the Heat Generation in the Pressure Vessel with no Thermal Shield

It will be noticed from Table A.1-1 that the major portion of the gamma currents at the blanket edge are due to sources in the core. Assuming a "straight ahead" approximation, the maximum angle with the normal becomes $\xi_{\max} = \sin^{-1} \frac{R_C}{R_B} = (42.7^\circ \text{ for the case of the small pressure vessel})$. Therefore in order to approximate the angular distribution of gammas arriving at the blanket edge from the core, a modified Lambert's Law was assumed, and the pressure vessel treated as a slab.

$$J(\xi) = J_B \cos 2\xi$$
$$\text{or } P(\xi)d\xi = 7.25 \cos 2\xi \sin \xi d\xi$$

$$\phi_r(r, \xi) = \int_0^{\pi/4} \frac{P(\xi) d\xi}{\cos \xi} e^{-\mu r}$$

$$G(\mathcal{N}) = (\mu - \sigma) E_\gamma k \int_{\xi=0}^{\pi/4} \int_0^{\pi/4} \frac{P(\xi) d\xi}{\cos \xi} e^{-\mu r}$$

$$x = r \cos \xi \quad \text{and} \quad d\mathcal{N} = 0 = \cos \xi dr - r \sin \xi d\xi$$

$$\begin{aligned} G(\mathcal{N}) &= 7.25 \int_0^{\pi/4} (\mu - \sigma) E_\gamma k \int_{\xi=0}^{\pi/4} \frac{(2 \cos^2 \xi - 1) \sin \xi d\xi}{\cos \xi} e^{-\mu r} \\ &= 7.25 \int_0^{\pi/4} (\mu - \sigma) E_\gamma k \int_{r=\mathcal{N}}^{1.415\mathcal{N}} \frac{2 \left(\frac{\mathcal{N}}{r}\right)^2 - 1}{r} e^{-\mu r} dr \end{aligned}$$

where $\mu - \sigma$ = energy absorption coeff, cm^{-1}

E_γ = gamma energy, mev

$k = 1.54 \times 10^{-8}$ BTU/hr cf/mev/sec cc

$G(\mathcal{N})$ = heat generation at x , BTU/hr cf

Numerical integration of the above is actually less tedious than an analytic solution. Using Simpson's rule and dividing the interval into four

$$\frac{G(\mathcal{N})}{\int_0^{\pi/4} (\mu - \sigma) E_\gamma k} = .25 \left\{ e^{-\mu \mathcal{N}} + 2.30 e^{-1.104\mu \mathcal{N}} + .614 e^{-1.207\mu \mathcal{N}} + .494 e^{-1.311\mu \mathcal{N}} \right\}$$

Except for large $\mu \mathcal{N}$ the heating curve is not much less than that obtained by assuming a collimated source. See Fig. A.4-1. A summation of all gamma heating curves is nearly linear on semi-log paper. See Fig. A.4-2.

In order to estimate the heating resulting from thermal neutron capture gammas in the iron, Chapman develops the following:

$$G(\mathcal{N}) = N_\gamma \frac{\int_0^{\pi/4}}{2} (\mu - \sigma) E_\gamma F(\beta, \mu t) k$$

where N_γ = number of gammas of energy E mev per capture

$F(\beta, \mu t)$ plotted Fig. 12 CF-54-10-76, Appendix D

$$\beta = \kappa/\mu$$

The following capture gamma sources were taken from Nucleonics May 1955:

.22 - 6 mev and .5 - 7.63 mev gammas per capture. β was taken as $\frac{.640}{.200} = 3.2$

for all energies, since μ does not vary appreciably for iron between 4-8 mev.

Again $1/1.2$ was used to estimate buildup. In order to estimate thermal

stresses, the sum of the two heating curves, that due to thermal neutron

capture gammas and that due to core and blanket gammas, was added and plotted

on semi-log paper. Approximating the curve by a straight line the expression

becomes $G(\mu) = G(0) e^{-m\mu}$.

A.5 - Estimation of Heat Generation in Pressure Vessel with a Thermal Shield

A thermal shield of thickness t_s attenuates the thermal neutrons, and hence the heating in the pressure vessel by a factor of $e^{-\kappa t_s}$. It was assumed that the gamma currents were attenuated as a collimated beam, thus slightly overestimating the current emerging from the thermal shield.

An expression for the emerging gamma current resulting from neutron capture in the thermal shield was developed, assuming slab geometry and isotropic emission of gamma photons.

$$S(y) = \phi_n(y) \sum_a N_{\gamma} = \pi h N_{\gamma} e^{-\kappa y}$$

$$dV = 2\pi r dr dy = 2\pi \rho d\rho dy$$

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VALUES OF $\frac{G(x)}{J(\mu-\sigma)E_x K}$ AS A FUNCTION OF ANGULAR DISTRIBUTION OF GAMMA CURRENT ENTERING SLAB

FIG A.4-1

G = HEAT GENERATION B/HRCF
 J = CURRENT ENTERING SLAB, $\delta/\text{CM}^2 \text{ SEC}$
 $\mu-\sigma$ = ENERGY ABSORPTION COEFF., CM^{-1}
 E_x = ENERGY OF INCIDENT GAMMA, MEV
 $K = 1.54 \times 10^{-8}$ B/HRCF / MEV/CC SEC

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HEAT GENERATION IN B/HR CF
VS DISTANCE FROM INNER SURFACE
OF PRESSURE VESSEL IN CM
(NO THERMAL SHIELD)

FIG A.4-2

$G(x)$ B/HR CF

APPROXIMATE HEAT GENERATION
 $G(x) \approx 315,000 e^{-.263x}$
TOTAL HEAT GENERATION

HEAT GENERATION DUE TO
ATTENUATING
CORE & BLANKET GAMMAS

HEAT GENERATION DUE TO
GAMMAS RESULTING FROM
THERMAL NEUTRON CAPTURE
IN PRESSURE VESSEL

X - DISTANCE FROM INNER SURFACE, CM

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FIG A.4-2

$$\begin{aligned}
J_0(t_s) &= \int_{VOL} S(r) dv e^{-\mu r} \frac{dA \cos \theta}{4\pi r^2} \frac{1}{dA} \\
&= \frac{K J_n N r}{2} \int_{r=0}^{t_s} \int_{\theta=t_s-r}^{\infty} e^{-K r} r d\theta dr e^{-\mu r} \frac{(t-r)}{r^3} \\
&= \frac{K J_n N r}{2} \int_{r=0}^{t_s} e^{-K r} E_{1,2}(\mu [t_s - r]) dr
\end{aligned}$$

However, in order to save time a conservative approximation to the self shielding was used, attenuating the gamma current by a factor of $e^{-\frac{\mu t_s}{2}}$.

$$J_g(t_s) \leq \frac{J_n N r}{2} (1 - e^{-K t_s}) e^{-\frac{\mu t_s}{2}}$$

All gamma heating and neutron capture heating curves were added as before and approximated by a simple exponential.

The blanket thickness and core diameter were fixed so that $R_p = 118 \text{ cm} =$ constant. A space of one inch was allowed between thermal shield and pressure vessel, and no account was taken of fission sources, thermalizing of neutrons, and thorium captures in this space. No area correction factor was used.

In general it is felt that all heating curves are conservatively estimated.

A.6 - Thermal Stress Calculation

As previously mentioned, all heating curves are treated as simple exponentials. In order to minimize thermal stresses it is expedient to hold the inner and outer faces of the pressure vessel at the same temperature by careful cooling. The temperature distribution in a spherical shell can be closely approximated by treating it as a slab, since the thickness is much less than the diameter.

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HEAT GENERATION IN B/HRCF
AS DISTANCE FROM INNER
SURFACE OF PRESSURE VESSEL IN
CM, AS A FUNCTION OF
THERMAL SHIELD THICKNESS, t_s

FIG A.5-1

FIG A.5-1

$$\frac{d^2\theta}{dx^2} + \frac{G(0)}{K} e^{-mx} = 0$$

$$\theta(x) = \frac{G(0)}{m^2 K} \left\{ \left[e^{-mx} - 1 \right] \frac{x}{t} + 1 - e^{-mx} \right\}$$

where $\theta(x)$ temperature difference, referred to either surface

$G(x) = G(0) e^{-mx}$, heat generation in BTU/hr cf

K = thermal conductivity of the pressure vessel, BTU/hr ft °F

t = thickness of the pressure vessel, ft

$$\theta_{ave} = \frac{1}{t} \int_0^t \theta(x) dx = \frac{G(0)}{m^2 K} \left\{ \left(\frac{1}{mt} + .5 \right) (e^{-mt} - 1) + 1 \right\}$$

The tangential tension resulting from thermal stresses at either surface of the pressure vessel can be closely approximated by the following expression:

$$\sigma_{TH} \approx \frac{\alpha E \theta_{ave}}{1 - \nu} \approx \frac{\alpha E}{1 - \nu} \frac{G(0)}{m^2 K} \left\{ .5 - \frac{1}{mt} \right\}$$

where α = linear coefficient of expansion, per °F

E = Young's Modulus

ν = Poisson's Ratio

A more rigorous treatment is presented in Timoshenko and Goodier, but the above approximation was checked numerically for one case and found to agree within about 3%.

A.7 - Mechanical Stress Calculation

Added to the tension at the two surfaces resulting from the radial temperature gradient is a mechanical stress resulting from the internal pressure. The hoop stress formula

$$\sigma_p = \frac{PR_i}{2t}$$

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MECHANICAL & THERMAL STRESSES
IN 9" PRESSURE VESSEL NS
THICKNESS OF THERMAL SHIELD

FIG A.7-1

FIG A.7-1

THERMAL SHIELD THICKNESS - INCHES 020 121

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gives results which are within 2% of those obtained by the more rigorous expression for tangential stress in a thick-walled hollow sphere. The pressure, p , was taken to be 4,000 psi, which should adequately represent the relief valve setting.

A.8 - Selection of Materials

The average blanket temperature was taken as 550°F, and cycle studies indicate that a maximum blanket temperature of 621°F was expected. With a three-inch thermal shield θ_{max} was found to be 24°, by differentiating the temperature expression and setting it equal to zero. Allowable stresses for the two steels in question (SA-212-B and SA-302-B) were taken from the ASME Boiler and Pressure Vessel Code, at a maximum temperature of 675°F. This should be adequate to allow for any "Poppendeik effect" (temperature rise due to the slower motion of the fissioning blanket fluid at the wall). A weld efficiency of 90% is required in calculating the design stresses.

A.9 - Heat Removal Required to Maintain Temperature Distribution Specified

In order to maintain both surfaces of the pressure vessel at the same temperature it is necessary to cool the outer surface. The heat flux can readily be calculated by differentiating the temperature expression at $x = r$.

$$\frac{q}{A} = -K \theta'(t) = \frac{G(t)}{m} \frac{1}{mt}$$

For the case of the small pressure vessel and the three-inch thermal shield the heat flux at the outer surface is 1540 BTU/hr ft². Because of the fact that there is a sizeable axial temperature gradient in the blanket, the cooling system must be carefully designed so that the outside of the pressure vessel will not be overcooled. A more realistic approach would be to recalculate completely the thermal stresses as a function of thermal shield

thickness for the larger pressure vessel, evaluating the expression developed in A.5, and letting the slope of the temperature curve go essentially to zero at $x = t$, the boundary implied for the case of natural cooling of the shell through three stagnant layers of air. It is felt that an acceptable vessel could probably be built with about 4-5" of thermal shield.

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APPENDIX B

PARAMETERS FOR NUCLEAR CALCULATIONS

B.1 - Basis of Calculation of Bare Spherical Reactor Size

The calculations for the critical size of a bare system were based on a Fermi age model, accounting for fast leakage using the critical equation:

$$1 = \frac{\eta p \epsilon f e^{-B^2 \tau}}{1 + L^2 B^2}$$

The fast effect (ϵ) was taken as unity and the resonance escape as 0.8. Thermal leakage ($1 + L^2 B^2$) was negligible in this calculation.

B.2 - Description of the Parameters for the 2-Group, 2-Region Calculations

Using UO_3 Slurry as Fuel

This description covers the cases investigated to obtain the curves shown in Figs. 2.4-3, 2.4-4, and 2.4-6.

Uranium Concentration in Blanket

From ORNL 1642 tables 14 and 15, the concentration of U-233 in the blanket leading to the minimum cost is 3.0 gm/kg Th. For 500 gm Th/liter the corresponding U-233 concentration is then 1.5 gm/liter.

Poison Fraction in Core

From ORNL 1642, Tables 14 and 15, a poison fraction of 0.075 neutrons absorbed in poison per U-233 atom destroyed in the core appeared to be reasonable. To allow for absorptions in additional fuel components a poison fraction of 0.08 was used.

Blanket Temperature

To avoid the large density changes in the blanket, it is necessary to keep the outlet temperature below about 750°F at 3000 psia (see Fig. 2.4-2). Assuming a ΔT of 300°F, the average blanket temperature becomes 550°F.

Core Temperature

The core outlet temperature was originally specified to be 1200°F, but since the temperature is a critical factor in the determination of age, it was decided to chose cases covering the range from present day operation at 600°F to the desired 1200°F. A ΔT in the core of 300°F was selected as being reasonable.

Temperature Ranges Selected

Outlet Temp.	900°F	1050°F	1200°F
Inlet Temp.	600°F	750°F	900°F
Average Temp.	750°F	900°F	1050°F

Note: The relationship between outlet and average temperatures is a function of flow rate vessel volume and flux. If the flow rate is small in comparison with the volume of the vessel, so that complete mixing occurs, the outlet and average temperatures will be essentially the same. If the flow rate is high, then there will be no mixing, and the average temperature will be midway between the inlet and outlet. The problem cannot be analyzed mathematically, so that empirical data must be compiled. In these preliminary calculations no mixing was assumed, because for high power removal high flow rates were envisioned. In the final cycle proposed, allowance was made for some mixing and for distortion of flux shape (inlet temp. 750, outlet 1200, and average 1050°F).

Reactor Size and Pressure

The basic assumption in setting these parameters was that it will be possible to fabricate a vessel no greater than nine inches in thickness. Using the code for pressure vessel design, sizes of 10'-0" at 3000 psi and 7'-0" at 4000 psi were selected.

In order to keep the thermal stresses in the vessel to about 4000 psi (pressure stress, 13,500 psi), it was assumed that a thermal shield of not more than ten inches in thickness would be required.

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PARAMETERS FOR 2-GROUP, 2-REGION CALCULATIONS ON ORACLE (Fuel- UO_2 slurry)

Case No.	Core Temp	Press (psi)	Core Radius	Blank Thick.	Vessel Dia.	Thorium Conc.	U Conc. in Blank	Core Age	Blank Age	Core Dif. Coef.		Blank Dif. Coef.	
										Fast	Therm.	Fast	Therm.
111	750°F	3000	102 cm	26 cm	10 ft.	500%	1.59%	2790	57.4	10.55	1.57	1.47	0.211
112	"	"	89	39	"	"	"	"	"	"	"	"	"
113	"	"	76	52	"	"	"	"	"	"	"	"	"
211	900	"	102	26	"	"	"	5890	"	15.32	2.28	"	"
212	"	"	89	39	"	"	"	"	"	"	"	"	"
213*	"	"	76	52	"	"	"	"	"	"	"	"	"
311	1050	"	102	26	"	"	"	8690	"	18.60	2.77	"	"
312*	"	"	89	39	"	"	"	"	"	"	"	"	"
313*	"	"	76	52	"	"	"	"	"	"	"	"	"
121	750	4000	56	26	7 ft.	"	"	515	55.8	4.53	0.674	1.45	0.208
122	"	"	43	39	"	"	"	"	"	"	"	"	"
123*	"	"	30	52	"	"	"	"	"	"	"	"	"
221*	900	"	56	26	"	"	"	2705	"	10.40	1.55	"	"
222*	"	"	43	39	"	"	"	"	"	"	"	"	"
223*	"	"	30	52	"	"	"	"	"	"	"	"	"
321*	1050	"	56	26	"	"	"	4400	"	13.25	1.97	"	"
322*	"	"	43	39	"	"	"	"	"	"	"	"	"
323*	"	"	30	52	"	"	"	"	"	"	"	"	"

Blanket Temp. 550°F.

* These cases are beyond the capabilities of the 2-group, 2-region code. No results were obtained. The curves in the nuclear section of the report indicate that the critical concentrations would be unreasonable.

Blanket Thickness

From the preliminary two-group calculations done by hand, a 24" blanket appeared to be more than adequate. Therefore, some blanket thicknesses less than 24" were selected in an attempt to find the best one. The thicknesses selected were: 10, 15, and 20" (26, 39, and 52 cm respectively).

B.3 - Calculations of the Fermi Age (τ) for a Solution of $\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5\text{H}_2\text{O}$ in D_2O (99.86 pure)

At high fuel calculations and for low moderator densities, it can be seen that the effect of the water of hydration on the age should be appreciable.

No simple method for taking into account the effect of the fuel on the age is available. The multi-group method outlined by M. Tobias (CF 54-9-97) for calculating the age of ThO_2 slurries in D_2O was used. This method employs the approximation:

$$\tau = \frac{\int_0^{u_{TH}} Z d\mu \int_{\mu}^{u_{TH}} \frac{d\mu'}{\Sigma_s(\mu') \Sigma_{TR}(\mu')}}{\int_0^{u_{TH}} Z d\mu}$$

$$= \frac{\sum_{i=1}^{L=Th} \frac{\Delta u_i}{\Sigma_s(\Sigma_s)_i(\Sigma_{TR})} \frac{Z_i \Delta u_i}{2} + \sum_{j=1}^{J=L=1} Z_j \Delta u_j}{\sum_{i=1}^{L=Th} Z_i \Delta u_i}$$

where Z = number of neutrons born per unit of lethargy between u and $u + du$.

The fission spectrum was broken up into 29 groups of lethargy intervals. It should be pointed out that the spectrum used was that for U-235, since no data were available for the U-233 spectrum.

To calculate $\xi \sum_s$ for each group it was necessary to account for the inelastic scattering properties of U-233 and Cr. Most of the information was not available, and it was necessary to estimate these properties. This approximation was made with the aid of L. Dresner. The approximation for $\sum_{\text{inelastic}}$ for Cr was obtained from "Physical Review" 95-1954. That for U-233 was rather crude, since only one point on the curve was known. The effect of these estimates on the final result was not of major importance, however, since the major change is due to the waters of hydration.

Fig. 2.4-7 is a plot of the results of this calculation, and shows age vs. fuel concentration for various densities of D₂O. It was assumed that the addition of the fuel caused no displacement of D₂O. It might be expected that a slight displacement would occur, but no data were available on the volume increase of the solution as the solvent is added.

Fig. 2.4-8 is a plot of age vs. W (molecular density of moderator) for various partial densities of the D₂O moderator. The density of the moderator can be corrected for displacement due to the addition of fuel (assuming that the displacement is known), and the age can be read directly.

B.4 - Description of Parameters for 2-Group, 2-Region Calculation using

UO₂Cr₂O₇.5 D₂O in D₂O Fuel

This description covers the cases investigated to obtain the critical concentration curves shown in Fig. 2.4-7.

Uranium Concentration in Blanket

As in Appendix B.2 the U-233 concentration is taken as 3.0 gm U-233/kg Th,

making the concentration of U-233 1.5 gm/liter for a thorium concentration of 500 gm/liter, and 3.0 for a thorium concentration of 1000.

Poison Fraction in Core

See Appendix B.2. In this case 0.075 was used because the fuel component absorptions were accounted for individually.

Poison Fraction in Blanket

Using information from CF 53-12-1, the ratio of $\frac{\sum_a^{Pa} + \sum_a^{24} + \sum_a^{F.P.}}{\sum_a^{O2}} = 0.04$ appears to be a reasonable estimate.

Blanket Temperature

Same as Appendix B.2.

Core Temperature

Same as Appendix B.2.

Reactor Size and Pressure

Using the same basic assumptions as in Appendix B.2, it was decided to study one reactor pressure of 3500 psia with a vessel 8'-9" (266 cm) in inner diameter.

The required thermal shield was estimated to be six inches.

Blanket Thickness

From the neutron balance of the UO_2 fuel cases, a 15" blanket (38 cm) appeared reasonable and was used in the chromate solution calculations.

Extrapolation Distance

To account for the effect of the iron thermal shield and the vessel wall without going to a three-region calculation, and extrapolation distance of 6" (15 cm) was used.

PARAMETERS FOR 2-GROUP, 2-REGION CALCULATIONS ON ORACLE
(Fuel: $UO_2Cr_2O_7 \cdot 5D_2O$)
Blanket Temp. 550°F

Case No.	Core Temp.	Press. (psi)	Core Radius	Blank. Thick.	Vessel Dia.	Thorium Conc.	U Conc. in Blank.	Core Age	Blank Age	Core Dif. Coef.		Blank Dif. Coef.	
										Fast	Therm.	Fast	Therm.
411		3500	80cm.	38cm.	8.75ft.	500 ^{gm} /%	1.5 ^{gm} /%	500	235	2.93	1.72	1.756	1.075
412		"	"	"	"	"	"	2000	"	5.85	3.44	"	"
413		"	"	"	"	"	"	3500	"	7.75	4.56	"	"
414		"	"	"	"	"	"	5000	"	9.23	5.44	"	"
415*		"	"	"	"	"	"	8000	"	11.70	6.88	"	"
416*		"	"	"	"	"	"	11000	"	13.70	8.09	"	"
421		"	"	"	"	1000	3.0	500	263	2.93	1.72	1.765	1.050
422		"	"	"	"	"	"	2000	"	5.85	3.44	"	"
423*		"	"	"	"	"	"	3500	"	7.75	4.56	"	"
424*		"	"	"	"	"	"	5000	"	9.23	5.44	"	"
425*		"	"	"	"	"	"	8000	"	11.70	6.88	"	"
426*		"	"	"	"	"	"	11000	"	13.70	8.09	"	"

* These cases are beyond the capabilities of the 2-group, 2-region code. No results were obtained. The curves in the nuclear section of the report indicate that the critical concentrations would be unreasonable.

B.5 - Calculation of Fermi-Age (τ) for a Solution of $\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5\text{H}_2\text{O}$ in H_2O

The method used to calculate the age of a D_2O solution (see Appendix B.3) cannot be satisfactorily applied to an H_2O solution because the relatively large "first flight" in H_2O essentially determines age.

The presence of the fuel in the water cannot be ignored when attempting to estimate the age. At the low water densities under consideration, the waters of hydration have an appreciable effect on the solution's age. A relatively simple approach was taken to account for this effect of the fuel.

The presence of the fuel was ignored except for the waters of hydration; this approach resulted in a new effective H_2O density (a function of fuel concentration), and the normal density ratio squared was applied to τ . It should not be inferred that the other components of the fuel (i.e., U, O, Cr) would not effect τ , but since the exact number of waters of hydration is an unknown, the approach taken appeared to be reasonable.

Figure 2.4-9 is a plot of the results of the calculation and shows age vs. fuel concentration for various moderator (H_2O) densities. (Critical concentration curves as superimposed as explained in the text of the report.)

B.6 - Description of Parameters for 2-Group, 2-Region Calculation Using

$\text{UO}_2\text{Cr}_2\text{O}_7 \cdot 5\text{H}_2\text{O}$ fuel in H_2O

This description covers the cases investigated to obtain the critical concentration curves shown in Fig. 2.4-10.

Uranium Concentration in Blanket

Same as Appendix B.4

Poison Fraction in Blanket

Same as Appendix B.4

Blanket Temperature

Same as Appendix B.2

Core Temperature

Same as Appendix B.2

Reactor Size and Pressure

With the use of the low alloy steel, SA302B, the vessel I.D. was increased to 10 feet at 3500 psi. Required thermal shield was calculated to be 3 inches.

Blanket Thickness

In an effort to increase the thorium absorptions, blanket thickness was increased to 24 inches (61cm.).

Extrapolation Distance

Same as Appendix B.4

PARAMETERS FOR 2-GROUP, 2-REGION CALCULATIONS ON ORACLE
 Fuel: $UO_2Cr_2O_7 \cdot 5H_2O$ in H_2O
 Blanket: ThO_2 slurry in D_2O at $550^\circ F$

Case No.	Core Temp	Press (psu)	Core Radius	Blank Thick.	Vessel Dia.	Thorium Conc.	U Conc. in Blank.	Core Age	Blank Age	Core Dif. Coef.		Blank Dif. Coef.	
										Fast	Therm.	Fast	Therm.
511		3500	80cm	61cm	10.0ft.	500%	1.5%	3000	235	9.5	1.62	1.756	1.075
512		"	"	"	"	"	"	3500	"	10.3	1.75	"	"
513		"	"	"	"	"	"	4000	"	11.0	1.86	"	"
514		"	"	"	"	"	"	4500	"	11.7	1.98	"	"
521		"	"	"	"	600	1.8	3000	240	9.5	1.62	1.758	1.070
522		"	"	"	"	"	"	3500	"	10.3	1.75	"	"
523		"	"	"	"	"	"	4000	"	11.0	1.86	"	"
524		"	"	"	"	"	"	4500	"	11.7	1.98	"	"
531		"	"	"	"	700	2.1	3000	245	9.5	1.62	1.760	1.065
532		"	"	"	"	"	"	3500	"	10.3	1.75	"	"
533		"	"	"	"	"	"	4000	"	11.0	1.86	"	"
534		"	"	"	"	"	"	4500	"	11.7	1.98	"	"

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Table B.6-1

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APPENDIX C
CALCULATION OF PROCESSING RATES

C.1 - Removal Rate of Xenon-135 from Core

The equation for buildup of Xe^{135} in the reactor core is

$$\frac{dN(x)}{dt} = y(x) \Sigma_f \phi - N(x) (\lambda + \phi \sigma_a(x) + R(x))$$

where $N(x)$ = Xenon concentration in main loop, atoms/cm³

$y(x)$ = Yield of Xe^{135} (or precursors), atoms/fission,

$R(x)$ = Number of reactor core loop volumes processed per day.

At steady state $dN/dt = 0$, and solving for the Xe^{135} concentration

$$N(x) = \frac{y(x) \Sigma_f \phi}{\lambda + \phi \sigma_a(x) + R(x)}$$

The Xenon poison level, P, maintained in the core loop is defined

$$P = \frac{\Sigma_a(\text{Xe})}{\Sigma_a(23)} = \frac{N(x) \sigma_a(x)}{\Sigma_f \left(\frac{\sigma_a(23)}{\sigma_f(23)} \right)}$$

Rearranging gives

$$N(x) = \frac{P \Sigma_f (\sigma_a(23)/\sigma_f(23))}{\sigma_a(x)}$$

Eliminating $N(x)$ and solving for the removal rate

$$R(x) = \frac{y(x) \phi \sigma_a(x)}{P (\sigma_a(23)/\sigma_f(23))} - (\lambda + \phi \sigma_a(x))$$

For the HRHR,

$$\sigma_a(x) = 2.4 \times 10^{-18} \text{ cm}^2/\text{atom @ } 550^\circ \text{F}$$

$$y(x) = 0.061$$

$$\phi = \phi_{\text{ave. core}} \times \frac{\text{mass in core}}{\text{mass in core loop}} = 1.8 \times 10^{14} \times \frac{1}{3.36} = 0.336 \times 10^{14}$$

$$\sigma_a(23)/\sigma_f(23) = 1.12$$

$$P = 0.01$$

$$\lambda = 1.81 \text{ day}^{-1}$$

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Substituting in the above values gives a processing rate of 29.1 reactor core loop volumes or 156 core volumes per day.

C.2 - Chemical Processing Rate of Core Solution

Denoting the fission product poison Groups (2) and (3) by L and H respectively, equations for their buildup in the reactor are:

$$\frac{dN(L)}{dt} = \gamma(L) \Sigma_f \phi - N(L) R$$

$$\frac{dN(H)}{dt} = \gamma(H) \Sigma_f \phi - N(H) (\sigma_a(H) \phi + R)$$

where N = concentration, atoms/cm³

γ = yield, atoms/fission

ϕ = average flux in core

R = fraction of reactor core volume processed per day.

At steady state, $dN/dt = 0$ and

$$N(L) = \frac{\gamma(L) \Sigma_f}{R/\phi}$$

$$N(H) = \frac{\gamma(H) \Sigma_f}{\sigma_a(H) \phi + R/\phi}$$

If U^{233} is considered the only fissionable isotope in the core,

$$\Sigma_f = \Sigma_a(23) \left[\sigma_f(23) / \sigma_a(23) \right]$$

$$\frac{\Sigma(H)}{\Sigma(23)} = \frac{\gamma(H) \sigma_a(H) \sigma_f(23)}{[\sigma_a(H) \phi + R/\phi] \sigma_a(23)}$$

$$\frac{\Sigma(L)}{\Sigma(H)} = \frac{\gamma(L) \sigma_a(L) \sigma_f(23)}{[R/\phi] \sigma_a(23)}$$

Because R/ϕ is much smaller than $\sigma_a(H)$, for all practical cases,

$$\frac{\Sigma(H)}{\Sigma(23)} = y(H) \frac{\sigma_f(23)}{\sigma_a(23)}$$

Since the total poison fraction,

$$T = \frac{\Sigma(H)}{\Sigma(23)} + \frac{\Sigma(L)}{\Sigma(23)} + P_{xe}$$

$$T = y(H) \frac{\sigma_f(23)}{\sigma_a(23)} + \frac{y(L) \sigma_a(L) \sigma_f(23)}{[R/\phi] \sigma_a(23)}$$

Solving for the processing rate,

$$R = \frac{y(L) \sigma_a(L) \phi}{(T - P_{xe}) \frac{\sigma_a(23)}{\sigma_f(23)} - y(H)}$$

For the HTHR,

$$\phi = 1.8 \times 10^{14} \text{ n/sec/cm}^2$$

$$T = 0.075$$

$$P_{xe} = 0.01$$

$$\sigma_a(23)/\sigma_f(23) = 1.12$$

$$\sigma(L) = 35 \text{ barns}$$

$$y(L) = 1.9$$

$$y(H) = 0.015$$

Substituting in the above values, gives a chemical processing rate of

0.0132 of a core volume per day or 0.00246 of a core loop volume per day.

C.3 - Processing Rate of Blanket Slurry

If the assumption is made that Th-233 decays instantaneously and that there is no Pa-233 in the blanket feed, the equation for the changes of Pa-233 and U-233 concentrations with time are as follows:

$$\frac{dN(13)}{dt} = N(02) \sigma_a(02) \phi - N(13) [\lambda + \sigma_a(13) \phi + R]$$

$$\frac{dN(23)}{dt} = N(13) - N(23) [\sigma_a(23) \phi + R]$$

where N = concentration in the blanket, atoms/cm³,
 ρ = ratio of total to thermal absorptions in Th-232,
 ϕ = average thermal flux in reactor blanket,
 R = fraction of reactor blanket volume removed for processing per day,
 λ = decay constant for Pa-233

At steady state, $dN/dt = 0$, and

$$N(13) = \frac{N(02)\sigma_a(02)\rho\phi}{\lambda + \sigma_a(13)\phi + R}$$

$$N(23) = \frac{N(13)\lambda}{\sigma_a(23)\phi + R}$$

Combining to obtain the isotopic ratio of U-233 to Th-232,

$$\frac{N(23)}{N(02)} = \frac{\rho\sigma_a(02)\phi\lambda}{[\lambda + \sigma_a(13)\phi + R][\sigma_a(23)\phi + R]}$$

In the HTHR, the U²³³/Th²³² ratio is 3 gm/Kg and the Th²³² concentration is 500 gm/l. Thus

$$\frac{N(23)}{N(02)} = \frac{1.5 \times 232}{233 \times 500} = 0.00299$$

Also, $\rho = 1.72$ (from nuclear data)

$$\lambda = .0253 \text{ day}^{-1}$$

$$\sigma_a(13) = 84 \text{ barns @ } 550^\circ\text{F}$$

$$\sigma_a(23) = 376 \text{ " "}$$

$$\sigma_a(02) = 5 \text{ " "}$$

$$\phi = 1.2 \times 10^{14} \text{ n/sec/cm}^2$$

Substitution of these values in the above equation and solving for R gives a processing rate of 0.0146 of a blanket volume per day or 0.0111 of a blanket loop volume per day.

APPENDIX D - HEAT EXCHANGERS

APPENDIX D.1

BOILER DESIGN PARAMETERS

SOUP [†] VELOCITY IN TUBES	74 FPS
SOUP DENSITY	7.4 LB/FT ³
VISCOSITY	0.07 LB/HR-FT
MASS FLOW	19.7×10^6 LB/HR-FT ²
REYNOLDS NO.	650,000
THERMAL CONDUCTIVITY	0.09 BTU/HR-FT-°F
NUSSELT NO.	1.4
FLOW AREA	0.504 FT ²
FILM COEFFICIENT OF HEAT TRANSFER	3030 BTU/HR-FT ² -°F
SCALE COEFFICIENT OF HEAT TRANSFER	1000 BTU/HR-FT ² -°F
BOILING COEFFICIENT OF HEAT TRANSFER	2000 BTU/HR-FT ² -°F
TUBE WALL	0.049 IN.
LOG AVG TUBE DIAMETER	0.326 IN.
TUBE CONDUCTIVITY	153 BTU-IN/HR-FT ² -°F
TOTAL HEAT TRANSFERRED	460×10^6 BTU/HR
OVERALL COEFFICIENT OF HEAT TRANSFER	460 BTU/HR-FT ² -°F
LOG MEAN TEMPERATURE DIFFERENCE	206 °F
REQUIRED HEAT TRANSFER AREA	4850 FT ²
TOTAL SOUP HOLDUP IN 2 BOILERS	658 LITERS
SOUP PRESSURE DROP	71 PSIA

† AVERAGE VALUES

APPENDIX D.2
BOILER SPECIFICATIONS

NO. OF BOILERS PER REACTOR - 2
HEATING SURFACE PER BOILER - 2500 FT²

BOILER SHELLS

2 REQUIRED
44" I. D.
4.1" WALL
21' ϕ LENGTH
CARBON STEEL
DESIGN TEMP - 700°F
DESIGN PRESS - 2700 PSIA

NOZZLE HEADS

16 REQUIRED
13 1/8" I. D.
1.7" WALL
STAINLESS STEEL
DESIGN TEMP - 1000°F
DESIGN PRESS - 3500 PSIA

BOILER SHELL HEADS

4 REQUIRED
44" ID
4.1" WALL
HEMISPHERICAL
DESIGN TEMP - 700°F
DESIGN PRESS - 2700 PSIA
CARBON STEEL

BOILER DRUMS

2 REQUIRED
42" I. D.
4" WALL
19' ϕ LENGTH
CARBON STEEL

TUBE SHEETS

16 REQUIRED
15.8" O. D.
3 1/3" THICK
STAINLESS STEEL
DESIGN TEMP - 1000°F
DESIGN PRESS - 3500 PSIA

DRUM INTERNALS & PIPING

20-14" OD CONICAL CYCLONES
WITH INCLINED SCRUBBERS PER BLR
12-8" ID C. S. RISER PIPES - 12' LONG
6-8" ID C. S. DWNCR PIPES - 12' LONG
2-12" ID C. S. DWNCR HDRS - 8' LONG

TUBES

2400 REQUIRED
3/8" OD X 0.049" WALL
PITCH - 0.625" Δ
22 FT AVERAGE LENGTH
STAINLESS STEEL
DESIGN TEMP - 1000°F
DESIGN PRESS - 3500 PSIA

ESTIMATED WEIGHT PER BOILER
102,500 LBS

TUBE SHEET NOZZLES

16 REQUIRED
13 1/8" I. D.
1 3/8" WALL
1' LONG
CARBON STEEL
DESIGN TEMP - 700°F
DESIGN PRESS - 2700 PSIA

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APPENDIX D.3 SUPERHEATER DESIGN PARAMETERS

SOUP [†] VELOCITY IN TUBES	100 FPS
DENSITY	4.67 LB/FT ³
VISCOSITY	0.07 LB/HR-FT
MASS FLOW	1.68×10^6 LB/HR-FT ²
REYNOLDS NO.	423,000
THERMAL CONDUCTIVITY	0.06 BTU/HR-FT-°F
NUSSELT NO.	1.4
FLOW AREA	1.18 FT ²
FILM COEFFICIENT OF HEAT TRANSFER	2850 BTU/HR-FT ² -°F
STEAM MASS FLOW (MAX) [‡]	249,000
REYNOLDS NO. (MAX) [‡]	149,500
NUSSELT NO.	1.4
EQUIV. DIAMETER [†]	0.042 FT.
SPECIFIC HEAT	1.82 BTU/LB-°F
EFFECTIVE COEFFICIENT OF HEAT TRANSFER	608 BTU/HR-FT ² -°F
SCALE COEFFICIENT OF HEAT TRANSFER	2000 BTU/HR-FT ² -°F
TUBE WALL	0.145 IN.
LOG AVG TUBE DIAMETER	0.334 IN.
TUBE CONDUCTIVITY	153 BTU-IN/HR-FT ² -°F
TOTAL HEAT TRANSFERRED	404.6×10^6 BTU/HR
OVERALL COEFFICIENT OF HEAT TRANSFER	227 BTU/HR-FT ² -°F
LOG MEAN TEMPERATURE DIFFERENCE	164.5 °F
REQUIRED HEAT TRANSFER AREA	10830 FT ²
TOTAL SOUP HOLDUP IN 5 SUPERHEATERS	1370 LITERS
SOUP PRESSURE DROP	108 PSI
ESTIMATED STEAM PRESSURE DROP	55 PSI

[†] AVERAGE VALUES

[‡] BASED ON MINIMUM FREE FLOW AREA AT SHELL & BETWEEN BAFFLES

APPENDIX D.4
SUPER HEATER SPECIFICATIONS

NO. OF SUPERHEATERS PER REACTOR 5
HEATING SURFACE PER SUPERHEATER 2170 FT²

SHELLS

5 REQUIRED
27" I.D.
3.5" WALL
17' Φ LENGTH
STAINLESS STEEL
DESIGN TEMP - 1100°F
DESIGN PRESS - 2700 PSIA

TUBE SHEET NOZZLES

20 REQUIRED
19 1/4" ID X 1' LONG
3.7" WALL
STAINLESS STEEL
DESIGN TEMP - 1100°F
DESIGN PRESS - 2700 PSIA

SHELL HEADS

10 REQUIRED
27" I.D.
3.5" WALL
HEMISPHERICAL
STAINLESS STEEL
DESIGN TEMP - 1100°F
DESIGN PRESS - 2700 PSIA

NOZZLE HEADS

20 REQUIRED
19 1/4" ID
4" WALL
HEMISPHERICAL
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500°F

TUBE SHEETS

20 REQUIRED
26.6" O.D.
8" THICK
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500 PSIA

BAFFLES

16 - HALF MOON SHELL
BAFFLES ON 1' SPACING
PER SUPERHEATER
BAFFLE HEIGHT - 14"
STAINLESS STEEL

TUBES

4900 REQUIRED
1/2" OD X 0.145" WALL
0.75" PITCH Δ
23 FT AVERAGE LENGTH
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500 PSIA

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APPENDIX D.5
REHEATER DESIGN PARAMETERS

SOUP [†] VELOCITY IN TUBES	75 FPS
DENSITY	5.61 LB/FT ³
VISCOSITY	0.08 LB/HR-FT
MASS FLOW	1.51×10^6 LB/HR-FT ²
REYNOLDS NO.	402,000
THERMAL CONDUCTIVITY	0.075 BTU/HR-FT-°F
NUSSELT NO.	1.4
FLOW AREA	0.24
FILM COEFFICIENT OF HEAT TRANSFER	3440 BTU/HR-FT ² -°F
STEAM VELOCITY	115 FPS
MASS FLOW	580,000 LB/HR-FT ²
REYNOLDS NO.	460,000
NUSSELT NO.	1.4
VISCOSITY	0.08 LB/HR-FT
EQUIV. DIAMETER	0.137 FT
FILM COEFFICIENT OF HEAT TRANSFER	523 BTU/HR-FT ² -°F
TUBE WALL	0.145 IN.
LOG AVG TUBE DIAMETER	0.334 IN.
TUBE CONDUCTIVITY	158 BTU-IN/HR-FT ² -°F
TOTAL HEAT TRANSFERRED	159×10^6 BTU/HR
OVERALL COEFFICIENT OF HEAT TRANSFER	248 BTU/HR-FT ² -°F
LOG MEAN TEMPERATURE DIFFERENCE	83.2 °F
REQUIRED HEAT TRANSFER SURFACE	7700 FT ²
TOTAL SOUP HOLDUP IN 4 REHEATERS	512 LITERS
SOUP PRESSURE DROP	110 PSI

† AVERAGE VALUES

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APPENDIX D.6
REHEATER SPECIFICATIONS

NO. OF REHEATERS PER REACTOR 4
HEATING SURFACE PER REHEATER 1925 FT²

SHELLS

4 REQUIRED
20" ID
1" WALL
30' ± LENGTH
STAINLESS STEEL
DESIGN TEMP - 1050°F
DESIGN PRESS - 460 PSIA

TUBE SHEET NOZZLES

16 REQUIRED
14" ID
1' LONG
2.5" WALL
STAINLESS STEEL
DESIGN TEMP 1050°F
DESIGN PRESS 460 PSIA

SHELL HEADS

8 REQUIRED
20" ID
1" WALL
HEMISPHERICAL
STAINLESS STEEL
DESIGN TEMP - 1050°F
DESIGN PRESS - 460 PSI

NOZZLE HEADS

16 REQUIRED
14" ID
3" WALL
HEMISPHERICAL
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500 PSIA

TUBE SHEETS

16 REQUIRED
20" OD
6" THICK
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500 PSIA

TUBES

2000 REQUIRED
1/2" OD X 0.145" WALL
0.75" Δ PITCH
34 FT AVERAGE LENGTH
STAINLESS STEEL
DESIGN TEMP - 1200°F
DESIGN PRESS - 3500 PSIA

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APPENDIX D.7

BLANKET HEAT EXCHANGER DESIGN PARAMETERS

SOUP [†] VELOCITY IN TUBES	12.3 FPS
DENSITY	78.2 LB/FT ³
VISCOSITY	0.484 LB/FT-HR
MASS FLOW	3,460,000 LB/HR-FT ²
REYNOLDS NO.	226,000
THERMAL CONDUCTIVITY	0.376 BTU/HR-FT-°F
NUSSELT NO.	0.95
FLOW AREA	0.48 FT ²
FILM COEFFICIENT OF HEAT TRANSFER	3900 BTU/HR-FT ² -°F
FEEDWATER VELOCITY	2.64 FPS
MASS FLOW	466,200 LB/HR-FT ²
REYNOLDS NO	167,500
NUSSELT NO	1.0
VISCOSITY	0.26 LB/FT-HR
EQUIV DIAMETER	0.0935 FT
FILM COEFFICIENT OF HEAT TRANSFER	1225 BTU/HR-FT ² -°F
SCALE COEFFICIENT OF HEAT TRANSFER	2000 BTU/HR-FT ² -°F
TUBE WALL	0.06 IN.
LOG AVG TUBE DIAMETER	0.44 IN.
TUBE CONDUCTIVITY	153 BTU-IN/HR-FT ² -°F
TOTAL HEAT TRANSFERRED	153.8 × 10 ⁶ BTU/HR
OVERALL COEFFICIENT OF HEAT TRANSFER	477 BTU/HR-FT ² -°F
LOG MEAN TEMPERATURE DIFFERENCE	32.7 °F
REQUIRED HEAT TRANSFER SURFACE	9880 FT ²

† AVERAGE VALUES

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APPENDIX D.8
BLANKET FEEDWATER HEATER
SPECIFICATIONS

NO. OF BLANKET EXCHANGERS PER REACTOR 5
 HEATING SURFACE PER EXCHANGER 1975 FT²

SHELLS

A - 5 REQUIRED
 21.5" I. D.
 2" WALL
 21.5' ϕ LENGTH
 CARBON STEEL

B - 5 REQUIRED
 29" I. D.
 2 $\frac{3}{4}$ " WALL
 2' ϕ LENGTH
 CARBON STEEL

C - 5 REQUIRED
 29" I. D. TO 21.5" I. D.
 (TRANSITION)
 2 $\frac{3}{4}$ " WALL
 2' ϕ LENGTH
 CARBON STEEL

A, B, C { DESIGN TEMP - 650°F
 { DESIGN PRESS - 2700 PSIA

TUBE SHEETS

10 REQUIRED
 25.5" O.D.
 5" THICK
 CARBON STEEL CLAD WITH SS
 DESIGN TEMP - 650°F
 DESIGN PRESS - 3500 PSIA

EXPANSION JOINTS

5 LABYRINTH TYPE
 (REFER FIG)

TUBES

3050 REQUIRED
 1/2" OD X 0.06 WALL
 27' LONG
 0.75" Δ PITCH
 STAINLESS STEEL
 DESIGN TEMP - 650°F
 DESIGN PRESS - 3500 PSIA

SHELL HEADS

A - 10 REQUIRED · DESIGN 3500 PSIA
 21.5" ID PRESSURE
 2" WALL DESIGN 650°F
 TEMP
 HEMISPHERICAL
 CARBON STEEL CLAD WITH SS

B - 5 REQUIRED
 29" I. D.
 2 $\frac{3}{4}$ " WALL
 HEMISPHERICAL
 CARBON STEEL
 DESIGN TEMP - 650°F
 DESIGN PRESS - 3500 PSIA

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BIBLIOGRAPHY

1. L. G. Alexander- "Schedule of Base Charges for Materials Sold or Leased by AEC", CF 55-4-170.
2. E. D. Arnold, A. T. Gresky, R. J. Klotzback, A. R. Irvine- "Preliminary Cost Estimation: Chemical Processing and Fuel Costs for a Thermal Breeder Reactor Power Station", ORNL-1761.
3. E. D. Arnold- "The Effect of Size of Thorex Plant on Processing Costs Associated with a Thermal Breeder Reactor Power Station", CF 55-3-40.
4. ASME Unfired Pressure Vessel Code.
5. R. B. Briggs- "Aqueous Homogeneous Reactors for Producing Central Station Power", ORNL-1642.
6. H. G. Carson and L. H. Landrum- "Preliminary Design and Cost Estimate for the Production of Central Station Power from an Aqueous Homogeneous Reactor Utilizing Thorium-Uranium-233"- NPG-112.
7. R. H. Chapman- "Analysis of Spherical Pressure Vessel Having an Energy Source Within the Wall", CF 54-10-76.
8. Crane Company- "Flow of Fluids".
9. P. R. Crowley, A. S. Kitzes- "Physical Properties of Thorium Oxide Slurries", CF 54-5-59.
10. Electrical World- June 29, 1953, McGraw-Hill Bulletin 3-548.
11. D. E. Ferguson, J. P. Sanders- "Economics of Chemical Processing for a Uranium-233 Breeder", CF 53-6-173.
12. S. Glasstone and M. Edlund- "Elements of Nuclear Reactor Theory".
13. P. N. Haubenreich- "Calculations for Thorium and Uranium Fueled Reactors", CF 53-12-1.

DECLASSIFIED

14. D. K. Holmes and R. V. Meghreblian- "Notes on Reactor Analysis-Part II-Theory", CF 54-7-88.
15. HRP Progress Reports- ORNL-1554, 1772, 1813, 1853.
16. Keenan and Keyes- "Thermodynamic Properties of Steam".
17. L. P. D. King- "Los Alamos Power Reactor Experiment and its Associated Hazards", LAMS-1611.
18. M. Lundin- "Cost Estimating Information-TBR Memo No. 4", CF 54-10-111.
19. L. S. Marks- "Mechanical Engineers Handbook".
20. McAdams- "Heat Transmission".
21. Nucleonics- May 1955.
22. S. Peterson and R. W. Stroughton- "Oak Ridge School of Reactor Technology Reactor Chemistry Text", CF 54-4-198.
23. Physical Review- Vol. 95 (1954).
24. Reactor Handbook- Vols. I, II, III.
25. Standards of Tubular Exchanger Manufacturers Association (TEMA).
26. Timoshenko and Goodier- "Theory of Elasticity".
27. M. Tobias- "Calculation of Fermi Ages for ThO₂-Heavy Water Slurries",
CF 54-9-97.
28. Vitro Corporation of America, Project Summary- D₂O Recovery (57-B)
KLX-1611 (April 1, 1953).

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