OAK RIDGE SCHOOL OF REACTOR TECHNOLOGY

REACTOR DESIGN AND FEASIBILITY PROBLEM

"AN 80 MEGAWATT AQUEOUS HOMOGENEOUS BURNER REACTOR"

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Preface

In September, 1956, a group of men experienced in various scientific and engineering fields embarked on the twelve months of study which culminated in this report. For nine of those months, formal classroom and student laboratory work occupied their time. At the end of that period, these eight students were presented with a problem in reactor design. They studied it for ten weeks, the final period of the school term.

This is a summary report of their effort. 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 issued 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.

The faculty joins the authors in an expression of appreciation for the generous assistance which various members of the Oak Ridge National Laboratory gave. In particular, the guidance of the group consultant, R. B. Briggs, is gratefully acknowledged.

Lewis Nelson
for
The Faculty of ORSORT
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We wish to acknowledge the help given us in the completion of this report by R. B. Briggs, our advisor, and the following members of the regular Laboratory Staff: A. L. Boch, J. R. Engel, D. E. Ferguson, E. E. Gross, B. A. Hannaford, P. R. Kasten, W. E. Kinney, U. Koskela, H. F. McDuffie, H. A. McLain, R. C. Robertson, D. S. Toomb, Jr., and M. E. Whatley. Thanks are also due the ORSORT faculty in preparing us to undertake and execute this report.
Abstract

This report describes an 80 Mw aqueous homogeneous burner reactor suitable for producing 20 Mw of electricity at a remote location. The reactor fuel consists of a light water uranyl sulfate solution which acts as its own moderator and coolant. The uranium is highly enriched (93% $^{235}$U). The primary considerations for the design were (1) simplicity and reliability of the components, (2) automatic demand control and safe for any load change, (3) full xenon override not required, (4) possibility of construction within the immediate future, (5) and economic operation not the controlling factor. Reasonably complete studies are presented for the reactor physics, safety, stability, chemistry, heat transfer, and operation of the system.
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CHAPTER 1 SUMMARY

1.1 Introduction

The results of a design study are submitted, based on the aqueous homogeneous burner reactor concept, for a nuclear power plant to produce 20,000 kw of electricity with 80% plant factor. The single region reactor, which is suitable for remote locations, uses an aqueous uranyl sulfate solution as the fuel, moderator, and coolant. Simplicity of design and reliability of operation are stressed more than economic considerations.

The essential objective of this report is to present a reasonable design based on present day technology with a minimum of component development. In the time available, a comprehensive study could not be performed which would produce any degree of optimization. Arbitrary decisions were frequently made in order to proceed with the announced purpose of the study. The design was carried only to the point of providing a flowsheet and preliminary designs for the major items of equipment. Preliminary layouts are included for the purpose of providing some basis for estimating the cost of the reactor plant.

1.2 Results

The choice of a light or heavy water fuel solution was not clearly pointed out by the elementary economic study of this reactor system. However, on the basis of the expected maintenance procedure of flooding the unit cells and the availability of D₂O at remote locations, the light water, fully-enriched fuel system was chosen for this reactor. The operating limits of the fuel were set as 260 to 290°C at 1750 psia in order to maintain solution stability at all times. The fuel solution, UO₂SO₄·H₂O, has sufficient copper sulfate added to recombine 100% of the radiolytic gases produced at full power of 80,000 kw of heat. The amount of Cu²⁺ (0.02 M) added to the solution is sufficient in this
case to prevent void formation in the core, thus eliminating a major safety consideration. It further reduces the explosion hazards on a dump and eliminates continuous letdown of gases to the low pressure system. Excess sulfuric acid (0.02 m) is added to the system to stabilize the copper sulfate, uranyl sulfate, and corrosion-product nickel.

Based on the available in-pile corrosion data, 6 kw/l power density at the core wall was chosen as being compatible with a corrosion rate of 3 mil/yr for stainless steel. From the total power requirement, and for an average power density of 43 kw/l, the core diameter was fixed as 5 ft with an average thermal flux of $1.2 \times 10^{14}$ neutrons/cm$^2$-sec. The pressure vessel, with a concentric inlet and outlet, is constructed of SA-320 base material with 18-8 Type 347 SS integrally clad to the inner surface. With an inside diameter of 6 ft the vessel is 3.3 in. thick of which 0.3 in. is the clad. Inside the vessel there are three equally spaced 18-8 Type 347 SS thermal shields each 0.187 in. thick of which 0.187 in. is corrosion allowance.

The cold-clean (20°C, no poisons) critical concentration was calculated as 12.8 g U$^{235}$/kg of H$_2$O (12.8 g/l) and the hot-clean (280°C, no poisons) as 14 g U$^{235}$/kg of H$_2$O (10.7 g/l). It is noted that the grams/liter decreases with increasing temperature. The temperature coefficient of reactivity has been calculated to be about $-1 \times 10^{-4}$/°C at 20°C and about $-4.2 \times 10^{-4}$/°C at 280°C. The results of calculations indicate that the reactor is stable and will operate in a safe manner.

The corrosion situation is not clearly defined for the reactor system. Some out-of-pile corrosion data seem to indicate that, after an initial high rate of attack, a protective stable film is formed on the exposed surfaces and then the attack is halted. Other data indicate that about 1 mil/yr might be
expected. At full power and assuming 1 mil/yr corrosion, about 1 to 1.2 lb/day of corrosion and fission products are deposited in the solution. The solids are concentrated in about 30 l/day of solution by the use of hydroclone separators and passed through a suitable process for removal of the solids and the soluble corrosion-product nickel before returning to the reactor system. It is felt that the nickel concentration must be kept below 0.03 m to insure solution stability. The process chosen for this operation is the Hydrogen Peroxide Precipitation Process presently under development at ORNL. It is expected that the nickel can be removed from the solution with 95 to 99% uranium recovery. If much lower corrosion rates are found to exist in the reactor, the continuous chemical reprocessing may be replaced by a batch operation or eliminated.

Since the approximately 95 g/day fuel burnup amounts to only 1/2°C drop in the average core temperature, it appears practical to replenish the burnup intermittently. In this manner the fuel feed pump can be operated for a short time every few days, thus greatly extending the service life of this critical piece of equipment. Fuel concentration and purge water are obtained by operating a condenser in conjunction with the pressurizer. It is estimated that sufficient decay heat is available to maintain the pressurizer temperature at about 300°C.

At full power and a fuel flow rate of 4.12 x 10^6 lb/hr, a total of 320,000 lb/hr of saturated steam at 515 psia (470°F) is produced in four heat exchangers. It is estimated that full power can be extracted by allowing the fuel temperature limits to be 253 and 296°C with one of the two circulating pumps out of service and the turbine operating at 480 psia. A cycle efficiency of 28.5% has been calculated for the steam cycle chosen for the reactor. The power cost has been estimated as 18 to 19 mills/kwhr.
1.3 Conclusions

It is concluded that the reactor described here is capable of fulfilling the desired characteristics of a nuclear power plant for isolated localities with certain qualifications. Present day technology is not sufficient to permit design, construction, operation, and maintenance of the system without further development. It is also concluded that some chemical treatment is required for this reactor in order to insure solution stability at all times.

The lack of a demonstrated technique for the maintenance of large aqueous homogeneous reactor systems is a serious situation and considerable effort needs to be devoted to the problem.

There exists a need for a more corrosion resistant material, perhaps titanium, from which large reactor systems can be fabricated to give complete confidence in their integrity.

Another need is to demonstrate whether or not it is possible to operate an aqueous homogeneous burner reactor for extended lengths of time (years) without chemical processing of the fuel solution to remove the soluble fission and corrosion products.
CHAPTER 2 TECHNICAL FEASIBILITY

2.1 Introduction

The aqueous homogeneous burner reactor is considered to be one of the many reactor types that might be suitable as a source of heat for an electrical generating station in remote locations. In particular, the reactor and its associated power generation equipment should be characterized by the following:

1) 20,000 kw of electricity with 80% plant factor.
2) Automatic demand control - safe under any load change condition.
3) Full xenon override not required.
4) Simplicity and reliability of design, operation, and maintenance based on present day technology.
5) Economic operation not the controlling factor.

Basically, the aqueous homogeneous burner reactor system is very simple. It consists of a single region reactor vessel in which the nuclear chain reaction takes place, a heat exchanger to remove the heat generated within the reactor vessel, and a pump to circulate an aqueous solution (or slurry) which contains fissionable material, moderator, and coolant. In practice, the simplicity of the system becomes complicated by the following facts:

1) The system must operate at a high temperature for reasonable steam conditions.
2) The system must operate with an overpressure to prevent boiling.
3) The circulating fluid is highly radioactive and very corrosive.
4) Solution stability must be maintained at all times.

The radioactivity and the corrosiveness of the fuel place a premium on the choice of materials and the manner in which the system is fabricated. Absolute
leak tightness must be assured throughout the system. To achieve this goal mechanical joints in the circulating system should be kept to a minimum. Induced radioactivity of the components places a high premium on the reliability of design, operation, and maintenance of the system.

The aqueous homogeneous reactor technology consists primarily of the developments of the Homogeneous Reactor Project at ORNL. The feasibility of this type reactor was demonstrated by the successful operation of the HRE at 1,000 kw of heat. Although not operated yet, the HRT, at 5,000 kw of heat, is expected to provide much needed information on maintenance and reliability of reactor components. To date there is no information on large scale reactor component reliability.

An aqueous uranyl sulfate solution was chosen as the fuel, with the enrichment and choice of light or heavy water to be determined. It was hoped that, by using the burner concept, chemical processing of the fuel would not be required. Fission and corrosion product removal by hydroclones appeared to be adequate. It was known that the system would have to operate with sufficient overpressure to prevent boiling. Oxygen pressurization was selected for the basis of this study with no attempt to justify the choice of this gas over steam.

The design of an aqueous homogeneous reactor is not too difficult in a nuclear sense. Sufficient data are available for making the physics calculations with reasonable confidence. Certain safety and stability criteria which have been developed for the HRT were available to evaluate these aspects of this reactor.

Since the design of aqueous homogeneous reactor systems is largely of a chemical nature, certain problems which deal with the chemical behavior of
the system must be investigated. Some of the more important problems which must be given consideration are discussed in the following paragraphs.

2.2 Solution Chemistry

The chemistry and stability of uranyl sulfate solutions under the proposed operating conditions have been thoroughly investigated and are reasonably well understood\(^{(1)}\)*. In the design of a reactor system for which a uranyl sulfate fuel solution is proposed, the chemical phenomena which must be considered are:

1) Two-phase separation.
2) Three-phase separation.
3) Uranium peroxide precipitation.
4) Hydrolytic precipitation.
5) Radiolytic decomposition of water.

Each of these will be discussed in the following paragraphs.

2.2.1 Two-Phase Separation. The solubility relationships of aqueous uranyl sulfate solutions are shown by Fig. 1. The solution is stable over a wide range of temperature and concentration conditions. A two-liquid-phase region lies above the homogeneous solution with the lower temperature limit of the two-phase region varying with concentration. Note that for very dilute solutions, the limiting temperature decreases with increasing uranyl sulfate concentration. The two-liquid-phase separation temperature is also affected by the nature of the solvent (light or heavy water), the presence of other solutes, and the excess sulfuric acid in the solution.

* Numbers appearing in parenthesis as superscripts refer to appropriate references in bibliography.
FIGURE 1
There is a large density difference between these two liquid phases in equilibrium. Therefore, in the two-phase region, a mixture of uranyl sulfate and water cannot be kept dispersed. Since the heavier phase is much more corrosive than the single phase solution and the presence of two phases of different densities will also affect the nuclear properties of the system, the formation of a two-phase solution must be prevented.

For a uranium concentration of about 12 g/l (of H2O), the possibility of separation of the uranyl sulfate solution into two liquid phases sets an upper limit of about 300°C for the temperature at which the reactor can be operated. For this reason, the average operating temperature of this reactor has been chosen as 275°C with a normal core inlet temperature of 260°C and an outlet temperature of 290°C. In the event a circulating pump fails, the outlet temperature may be increased to 300°C and the inlet temperature may be decreased to 250°C. The reactor will then produce 80% of its rated power with one pump.

2.2.2 Three-Phase Separation. The actual composition of the fuel solution in the reactor is changed continuously by the accumulation of corrosion and fission products. The phase stability of the uranyl sulfate solution may be altered by the increase in concentration of any or all of these products. The information available is limited to those solution systems considered to be the most important.

The solubility of UO3 is affected by the total amount of sulfate ion in solution\(^2\). If it is assumed that the sulfate ion comes from the sulfuric acid (an additive), then for a uranyl sulfate solution (UO2SO4), the ratio UO3/H2SO4 is the same as the ratio UO3/SO4 or UO3/SO3. For the case of the neutral salt, this is equal to unity.

For a fuel solution of a fixed composition, 0.055 m UO2SO4, 0.02 m
CuSO₄, and 0.02 m H₂SO₄ (excess), the ratio UO₃/H₂SO₄ is equal to

\[
\frac{0.055}{0.055 + 0.02 + 0.02} = 0.58.
\]

The need for the 0.02 m CuSO₄ and 0.02 m H₂SO₄ will be discussed later.

Fig. 2 shows the solubility of UO₃ in H₂SO₄-H₂O solution from 150°C to 290°C. It is seen that for a given H₂SO₄ acid concentration, the saturation temperature of the uranyl sulfate solution increases as the ratio of UO₃/H₂SO₄ decreases. This then gives a clue as to what might be expected if the SO₄ ion concentration in the solution is increased.

2.2.3 Effect of Ni and H₂SO₄ Concentrations. It will be assumed at this point that the fuel solution is of the concentration stated in the previous paragraph with an H₂O solvent. As the reactor is operated, corrosion and fission products build up and reduce the H₂SO₄ content. Thus, H₂SO₄ has to be added periodically to maintain the specified concentration.

Of the corrosion products NiSO₄ is of particular interest. NiSO₄ is soluble and does not precipitate out until its concentration is quite high.

Fig. 3 (Ref. 3) is a phase diagram of a system composed of the constituents of interest, but of different concentrations. Consider a solution such as described by the figure with a 0.02 m CuSO₄ concentration. Note that as the NiSO₄ concentration builds up, the second liquid phase appears at a temperature just over 300°C. If the temperature were held between 260°C and 290°C, and the NiSO₄ concentration increased above 0.015 m, formation of a solid phase containing uranium can be expected. This is undesirable, to say the least, although the precipitate can be removed in the hydroclones and the lost uranium replaced.

Obviously, the above situation can not be tolerated. Experimental work
LEGEND:
- △ 290°C
- ▲ 270°C
- □ 250°C
- ● 200°C
- ○ 150°C

Fig. 2 - Solubility of UO₃ in H₂SO₄-H₂O Solution
Fig. 3 - The Effect of CuSO$_4$ and NiSO$_4$ on Phase Transition Temperatures.
(0.04 m UO$_2$SO$_4$, 0.01 m H$_2$SO$_4$)
at ORNL (4) indicates that by increasing the H$_2$SO$_4$ concentration, the solidus lines are displaced upward and to the right.

Returning to the system of interest for the Homogeneous Burner Reactor, a 0.02 m H$_2$SO$_4$ concentration seems to be adequate to provide the necessary range of solution stability. The limited data available at this time indicates that this solution will not precipitate with a NiSO$_4$ concentration of 0.03 m. Further, the temperature at which two liquid phases appear is a minimum (315°C) for this nickel concentration.

Thus, it was decided to maintain a 0.02 m concentration of H$_2$SO$_4$ and to limit the NiSO$_4$ concentration to 0.03 m. This then establishes the maximum fuel solution temperature at 315°C. Note that if the H$_2$SO$_4$ concentration is decreased, the temperature at which two-phase separation occurs is also decreased; also the region of possible precipitation is approached.

2.2.4 Uranium Peroxide Precipitation. Hydrogen peroxide is produced in the reactor fuel solution by the radiation induced decomposition of water. The peroxide concentration in the solution decreases with increasing temperature. Above 200°C the peroxide decomposition rate is so large that only O$_2$ is found in the solution. Under the normal operating conditions of this reactor, no trouble is expected from the precipitation of peroxides. However, if the reactor is operated at low temperature and at moderate power levels, the peroxide can react quantitatively with the uranium to form uranyl peroxide:

$$\text{H}_2\text{O}_2 + \text{UO}_2^{++} \rightarrow \text{UO}_4^+ + 2\text{H}^+.$$  

Uranyl peroxide has a low solubility and precipitates out of solution causing phase instability. This precipitation can be suppressed with the use of a homogeneous catalyst to recombine the radiolytic gases. A combination of Cu$^{++}$ and Fe$^{++}$ has proven to be very effective.
To prevent peroxide precipitation in this reactor, the start-up procedure requires that the solvent in the main loop be heated to about 200°C before adding fuel solution from the dump tanks. The reactor is made critical at this temperature. It is then raised to the operating point by concentrating the fuel solution.

2.2.5 Hydrolytic Precipitation. Uranyl sulfate solutions are acidic because of hydrolysis. The pH of the solution at room temperature decreases as the concentration of uranyl sulfate is increased. Dilute solutions at elevated temperatures (250-300°C) are hydrolyzed to such an extent that as much as 25% of the uranium may be precipitated as hydrated uranium trioxide. Sulfuric acid must be added to the fuel solution to suppress hydrolysis at operating temperatures in this range.

For this reactor system, an excess of acid is maintained to prevent this precipitation as well as any possible hydrolysis of the copper catalyst in the solution.

2.2.6 Radiolytic Decomposition of Water. When water is exposed to ionizing radiation, radiolytic hydrogen and oxygen are produced. The gases produced are rapidly recombined by the use of a homogeneous copper catalyst when the system is at elevated temperatures (5). The activity of the catalyst is greater in light water than in heavy water systems. For this reactor, the concentration of the copper catalyst (0.02 m) is such that 100% internal recombination is obtained at full power.

2.3 Corrosion

The general effect of corrosion on the stability of the fuel solution has been discussed in Sec. 2.2.3. A more detailed discussion follows.
To a large extent the limitations which are imposed on the design and operation of a reactor using an aqueous uranyl sulfate fuel solution are (1) those resulting from maintaining fuel solution stability, and (2) those brought about by the severe corrosive nature of the fuel solution. There appear to be few variables on which the corrosiveness of the solution does not depend. Some of the more important variables are fuel concentration, excess acid and oxygen in the solution, temperature, fluid velocity, other metal sulfates in the solution, reactor power density, and even the corrosion products themselves. An additional effect may come from changes in the physical characteristics of the container material caused by long term irradiation in the reactor system.

The principal structural material chosen for components in contact with the fuel solution is 18-8 Type-347 SS. However, it is felt that 18-8 Type-304 ELC SS with a grain size of 7 or 8 will be a satisfactory substitute. The advantage in using these materials is the elimination of heat treatment after welding in order to avoid sensitization. For certain application where high velocities and possible erosion may be encountered, for example the pump impellers, hydroclones, and jet pumps, titanium is a more suitable material and is recommended.

The corrosion protection of stainless steels depends on the formation of a stable protective film. The effect of the principal corrosion variables and some of the precautions taken to minimize the attack are discussed in the following paragraphs.

2.3.1 Temperature Effects. In the temperature range of 150 to 225°C the corrosion rate increases steadily with temperature. It reaches a maximum value at some temperature in the vicinity of 225°C and then starts to decrease. At
250°C, the corrosion rate is appreciably less than the maximum and continues to decrease with increasing temperature to the point at which two-phase separation occurs. At the higher temperatures (250 to 300°C), corrosion occurs initially at an appreciable rate and then approaches a constant and much lower value. This indicates a possible pretreatment to development a stable film, after which reactor operation may proceed with a very low corrosion rate. Some long term tests have indicated corrosion rates of the order of 0.1 mil/yr for 10 g/l solution in the range 250 to 300°C. (6)

As a consequence of the lower corrosion rates at the higher temperatures, the circulating pumps in this reactor have been placed in front of the heat exchangers.

2.3.2 Velocity Effects. In the reactor operating temperature range, the effect of velocity and of turbulence are important considerations. For a given fuel concentration, the corrosion rate is practically constant with respect to velocity until a certain critical velocity is reached. At this velocity, the corrosion rate increases rapidly and will quickly cause failure. Above this velocity the protective film is apparently not stable and is continuously swept away.

The critical velocity is affected by both fuel concentration and temperature (7). As the uranyl sulfate concentration increases, the critical velocity decreases and, conversely, as the temperature increases, the critical velocity increases. Table I gives critical velocities as a function of temperature for a uranyl sulfate concentration of 12 g/l.
TABLE I. Critical Fuel Velocities for 12 g/l Solution

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>Critical Velocity (ft/sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>250</td>
<td>25-35</td>
</tr>
<tr>
<td>275</td>
<td>30-40</td>
</tr>
<tr>
<td>300</td>
<td>40-50</td>
</tr>
</tbody>
</table>

The primary loop piping has been sized such that the fuel velocity does not exceed the values given above. A maximum velocity for the piping was set at 22 ft/sec and 12 ft/sec for the heat exchanger tubes. A corrosion allowance of 0.1 in. was included in sizing the pipes and major components, with the exception of the core and heat exchanger tubing. The core vessel has a 0.3 in. clad surface while the heat exchanger tubing has a 0.030 in. corrosion allowance.

2.3.3 Acid Additions. The effect of excess acid is to increase the corrosiveness of the solution. Increasing the uranyl sulfate concentration has the same effect. Hence the corrosion rates are increased and the critical velocities are decreased when the acid and fuel concentrations are increased. The results of some loop tests with 0.04-0.05 m H₂SO₄ showed excessive corrosion rates, and it is questionable whether or not systems with this large an acid concentration are operable for extended lengths of time (7). The 0.02 m H₂SO₄ concentration specified for this reactor is expected to give reasonable corrosion rates.

2.3.4 Stress Corrosion. Experiments have shown the absence of stress corrosion cracking of stainless steel and titanium when exposed to uranyl sulfate solutions. However, positive evidence of the absence of stress corrosion
cracking in these materials when subjected to fuel solutions containing small quantities of the halides is not yet available. The much discussed stress corrosion cracking of the HRT leak detector system resulted from the presence of chloride ions which were presumably left in the tubing from an improper cleaning procedure\(^{(8)}\). Rigid inspection is necessary to circumvent recurrence of this type failure. The boiler feed water must have very rigid control on purity in order to minimize the danger of stress corrosion cracking in the heat exchangers.

**2.3.5 Radiation Corrosion.** The effect of radiation on corrosion is in general to increase the rate. This can become a major factor in determining the size of the reactor core vessel. In general, as the power density adjacent to the vessel wall increases, the corrosion rate increases but not necessarily in a linear manner.

Some recent in-pile experiments\(^{(8)}\) which operated at 280°C with 0.04 m \(\text{UO}_2\text{SO}_4\) (10 g/l concentration) and 0.02 m \(\text{H}_2\text{SO}_4\) showed an over-all corrosion rate of about 0.4 mil/yr for stainless steel. Power densities for the corrosion coupons ranged from 4.7 kw/l for the leading coupon to 0.9 kw/l for the rear coupon. The power density in the nose of loop core was about 6.2 kw/l. In a similar test where the fuel concentration has been increased to 0.17 m \(\text{UO}_2\text{SO}_4\) and the acid to 0.03 m \(\text{H}_2\text{SO}_4\), the over-all corrosion rate was about 1.3 mils/yr.

It has been noted that corrosion specimens placed outside the high flux region of the in-pile loops do not show appreciably different corrosion rates than those specimens run in out-of-pile loops. This is a fortuitous situation for the external system design can be based on out-of-pile corrosion data.
From the meager in-pile data on stainless steel, a power density at the core wall of 6 kw/l was selected as a reasonable value on which to base the core diameter. It is expected that this power density will result in less than 3 mils/yr corrosion for the core vessel. The core vessel has been designed to have three 1-inch concentric thermal shields, each of which has 0.093 corrosion allowance on each side. The pressure vessel will have 0.30 in. stainless clad surface in contact with the solution. It is expected that the innermost thermal shield will be the only one for which the 3 mils/yr corrosion rate will apply. The power density at the other shields should be many orders of magnitude lower than that at the inside surface of the thermal shield.

2.4 Chemical Processing

Corrosion and fission products, both soluble and insoluble, must be removed to insure a longer useful life to the fuel solution. This requires that the solution be removed from the system, reprocessed, and returned to it. Chemical control of all these operations must be established. A more detailed analysis of these problems follows.

2.4.1 Removal of Soluble Corrosion and Fission Products. There is some uncertainty as to whether or not the limiting concentration of nickel, as stated under Section 2.2.3, will be reached. It depends on how the system behaves corrosion-wise. Table II shows the time it will take for this system to reach the 0.03 m Ni\textsubscript{3}O\textsubscript{4} level, as a function of the expected corrosion rates. In this calculation it is assumed that the concentration of UO\textsubscript{2}\textsubscript{3}O\textsubscript{4}, H\textsubscript{2}SO\textsubscript{4}, and CuSO\textsubscript{4} will remain constant.
If the concentration of NiSO₄ is to be kept at a lower limit, it is necessary to reprocess the solution, either continuously or periodically. Several methods have been proposed for the removal of the soluble corrosion and fission products. The Hydrogen Peroxide Precipitation Process under development at ORNL⁹ appears very promising. Depending upon the number of times the treated solution is recycled, the uranium recovery is very good, ranging from 95 to 99%.

2.4.2 Removal of Insoluble Corrosion and Fission Products. With the exception of nickel, samarium, and cesium, all corrosion and fission products are more or less insoluble at the conditions of temperature, pressure, and composition of the solution in the system. From the standpoint of the chemical stability of the solution, samarium and cesium are not too important due to their small production and, therefore, low concentration. The effect of Ni was discussed in Section 2.2.3.

The large amount of insoluble corrosion and fission products expected to be produced during the operating of this reactor requires that they be removed continuously to avoid their buildup in the system and the danger of plugging the heat exchanger tubes. Table III shows the amount of corrosion

---

### TABLE II. Days Required to Reach 0.03 m NiSO₄ Level

<table>
<thead>
<tr>
<th>Average Corrosion Rate (mil/yr)</th>
<th>Operating time (days)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2</td>
<td>297</td>
</tr>
<tr>
<td>1.5</td>
<td>396</td>
</tr>
<tr>
<td>1.0</td>
<td>594</td>
</tr>
<tr>
<td>0.5</td>
<td>1188</td>
</tr>
</tbody>
</table>

---
products produced per day and the equivalent increase of concentration in solution based on an average corrosion rate for the entire system.

**TABLE III. Daily Production of Corrosion Products**

<table>
<thead>
<tr>
<th>Average Corrosion Rate - mil/yr</th>
<th>Fe (lb)</th>
<th>Fe (g/l)</th>
<th>Cr (lb)</th>
<th>Cr (g/l)</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.0</td>
<td>1.24</td>
<td>0.056</td>
<td>0.292</td>
<td>0.0132</td>
</tr>
<tr>
<td>1.5</td>
<td>0.93</td>
<td>0.042</td>
<td>0.219</td>
<td>0.0099</td>
</tr>
<tr>
<td>1.0</td>
<td>0.62</td>
<td>0.028</td>
<td>0.146</td>
<td>0.0066</td>
</tr>
<tr>
<td>0.5</td>
<td>0.31</td>
<td>0.014</td>
<td>0.073</td>
<td>0.0033</td>
</tr>
</tbody>
</table>

The daily fission product production and the corresponding increase in concentration for the system operating at full power are shown in Table IV.

**TABLE IV. Daily Fission Product Production**

<table>
<thead>
<tr>
<th>Fission product</th>
<th>grams</th>
<th>g/liter</th>
</tr>
</thead>
<tbody>
<tr>
<td>Xe</td>
<td>0.001752</td>
<td>1.75 x 10^{-7}</td>
</tr>
<tr>
<td>Sm</td>
<td>0.0192</td>
<td>1.92 x 10^{-6}</td>
</tr>
<tr>
<td>LCSFP*</td>
<td>96,000</td>
<td>9.6 x 10^{-3}</td>
</tr>
</tbody>
</table>

*low cross section fission products

The corrosion products, Fe^{3+} and Cr^{3+} (normally present in the +6 state but is reduced by radiation to the +3 state), will probably appear first in the protective film and later, as a result of thermal shock, in the solution. There ions undergo hydrolysis very rapidly under the operating conditions of the reactor. The solution becomes saturated and the oxides will precipitate when the concentration is on the order of 3 to 10 ppm. After the solution
becomes saturated, the corrosion products must be removed constantly to prevent their buildup.

The solubility of fission products is greater than for the corrosion products. The fission product concentration in the solution is expected to be on the order of 60 to 120 ppm. Even though this solubility is small, saturation of the solution takes some time due to the small rate of production of fission products in the core. For this system, the solution will be saturated with fission products after about 70 days of operation. After this time they must be removed from the system as soon as they are formed to prevent their buildup.

The removal of insoluble solids is by means of hydroclones. It was stated in Section 2.3 that corrosion proceeds at a high rate at the start of operation and then levels off to a smaller, but constant, rate thereafter. This means that the amount of insoluble, as well as soluble, corrosion products produced will be large during this period. It is proposed to run this reactor for several weeks with a fuel solution similar to the one that will be used for actual power generation, but using natural uranium in the uranyl sulfate solution. Once the system is conditioned in this way, it will be drained and the true fuel solution injected. It is hoped that this technique will condition the system such that a much lower corrosion rate is obtained under operating conditions.

On the basis of a corrosion rate of 1.0 mil per year, the amount of insoluble material produced per day will be 0.960 lb. In order to treat the volume of the whole system and remove the solids produced in one day, it is necessary to use four 0.4-in. hydroclones in series. This size hydroclone has been tested at ORNL with slurries having particle sizes ranging from
0.2 μ to 2 μ, or about the same size range of the corrosion product particles. For a particle size of about 0.5 μ the efficiency of removal is about 50% and with the pressure drop available, the capacity of a hydroclone of this size is about 0.8 gpm. Underflow pots are provided to receive and concentrate the solids removed from the main loop of the system.

2.5 Chemical Control

The operation of this reactor can be controlled by means of the relationship that exists between the average temperature and the critical concentration of the fuel solution, as determined from nuclear considerations. This method is sensitive to the buildup of fission and corrosion products, and only provides information about the performance of the high pressure loop. It is necessary to take periodic samples at various points in the system to assure an adequate control.

It is proposed to obtain samples of the fuel solution from the following stations:

1) High pressure system,
2) Low pressure system,
3) Hydrogen peroxide uranium recovery system.

The fuel solution should be analyzed to determine the following: total uranium, nickel, copper, pH, sulfate, and suspended solids.

Gas samples must be obtained from the stream leaving the pressurizer and going into the condenser, and from the stream entering the primary recombiner-condenser above the dump tanks. Each sample must be analyzed for oxygen and hydrogen content.

2.5.1 Sampling Devices. A device for obtaining samples of highly radioactive uranyl sulfate solutions was developed at ORNL for use in the HRT(11).
Samples of liquid and suspended solids are taken from the high pressure system by allowing the solution to pass through the sampler to the low pressure dump tanks. Samples from the low pressure system are taken by pressurizing the dump tanks and forcing liquid through the samplers into a fuel transfer tank.

A heavily shielded sample station is provided. This station contains two isolation chambers, one for isolating samples from the low pressure system and the other for obtaining samples from the high pressure system. Each chamber in the station is served by a common loading and manipulating device.

Apparently, the main limitation of this device occurs when solutions with a very high solid content are sampled. A complete description of the sampling operation is given in the reference cited above. So far, no gas sampling device has been developed to obtain samples of radioactive gas mixtures.

2.5.2 Analytical Facilities. Since the materials to be analyzed are highly radioactive, the analysis must be performed remotely. A hot cell must be provided which is well equipped with the necessary instruments to carry on the analytical determinations. This hot cell must be divided into at least 3 compartments, one of which should serve as the location for the shielded sampling station. Special remotely-operated instruments have been developed (12).

The methods of analysis for the different constituents are fully developed and published (13).

2.5.3 Analytical Results Evaluation. The frequency with which samples are taken and analyzed will probably decrease as the time of operation increases. It is expected that during the first month of operation, samples will be taken and analyzed at least once every day; thereafter samples for complete analysis will be taken once a week.
Among the determinations to be made, pH and suspended solids should give a fairly good indication of the reactor performance, and therefore should be the basis of the daily control of the reactor. Once a given value for the pH of the solution is agreed upon, the concentration of the free sulfuric acid can be kept constant. Abnormal variations in suspended solids will give, together with the pH figure, some indication of the degree of corrosion and of the operation of the hydroclone station.
CHAPTER 3  NUCLEAR FEASIBILITY

3.1 Nuclear Systems

The Aqueous Homogeneous Burner concept was chosen for this reactor study for the reasons pointed out in Section 2.1. Inasmuch as the fuel and moderator enrichment were not specified, four systems were considered. These consisted of both light and heavy water uranyl sulfate solutions with a $^{235}U$ isotopic enrichment of 20% and 93%. A spherical core was chosen in the belief that a smaller loop inventory would be obtained. For the purpose of most of the nuclear calculations, the spherical core was considered to be surrounded by a 6-in. thick iron pressure vessel.

3.2 Reactor Physics Calculations

In general, the preliminary calculations consisted of the bare-pile type using the equation

$$k_{eff} = \frac{K_T p_T}{(1 + L_B^2)(1 + \gamma B_g^2)}$$

(3.2.1)

where

- $k_{eff}$ = effective multiplication constant of the system, dimensionless
- $p_T$ = resonance escape probability, dimensionless
- $\gamma$ = one-sixth of the mean square slowing down distance of fission neutrons to thermal energy, cm$^2$
- $L_B^2 = D_T/\Sigma_{aT}$ = square of thermal diffusion length, cm$^2$
- $D_T$ = thermal diffusion coefficient, cm
- $\Sigma_{aT}$ = macroscopic thermal absorption cross section, cm$^{-1}$
- $B_g^2$ = geometric buckling of the equivalent bare pile, cm$^{-2}$
- $K_T = \gamma/\Sigma_{TT}/\Sigma_{aT}$, neutrons produced per neutron absorbed at thermal
\[
\frac{V}{A} = \text{average number of neutrons born per fission}
\]
\[
\Sigma_T = \text{macroscopic thermal fission cross section, cm}^{-1}.
\]

Critical concentrations were determined for the cold-clean (20°C average core temperature) and the hot-clean (280°C average core temperature) conditions. The pertinent results of these calculations are shown in Fig. 4, 5, and 6 where the critical concentrations and critical masses are plotted as a function of the core size.

The total thermal power requirements and the corrosive nature of uranyl sulfate solutions suggest a core diameter in the 4 to 7-ft range. In this range of diameters, the light water system with 20% enriched fuel gave critical concentrations of about 65-80 g/l. Corrosion limitations (see Sec. 2.3.2) made this system unacceptable; therefore it was dropped from further consideration.

3.3 Nuclear Parameters

The diffusion lengths for the heavy water solutions were obtained from the work of Noderer. The values used for one-sixth the mean square slowing-down distance to thermal for fission neutrons in light and heavy water were obtained from the experimental results tabulated in the Reactor Handbook. The results of Deutsch were used to obtain the thermal diffusion lengths in light water. Other nuclear parameters were calculated from data compiled by Hughes and Harvey.

3.4 Oracle Calculations

Three-group, two-region Oracle calculations were performed on the light water uranyl sulfate highly enriched system to provide typical flux plots and to provide check points for the hand calculations obtained by use of Eq. (3.2.1). A constant preparation routine generated the
Fig. 4 - Critical Concentration vs Radius for UO₂SO₄-
H₂O System
Fig. 5 - Critical Concentration vs Diameter for UO$_2$SO$_4$ - D$_2$O System
Fig. 6 - Critical Concentration vs Diameter for UO$_2$SO$_4$ - D$_2$O System
necessary nuclear parameters for the Oracle calculations from the raw input data. The procedure and theory of the method are adequately described by Bate, et al.\(^{(18)}\). These calculations were performed for 4, 5, and 6-ft diameter cores in both the cold-clean and hot-clean conditions. The Oracle calculations differed from the hand calculations by less than 10%, this established the validity of Eq. (3.2.1) for these reactor systems.

3.5 Core Size

Calculations were performed to obtain the critical fuel concentration of each reactor system as a function of core size. These calculations were carried out for the cold-clean and hot-clean conditions. The results are shown by Fig. 4 for the light water system and by Fig. 5 and 6 for the heavy water systems.

A typical flux plot is shown by Fig. 7 for the three-group, two-region Oracle calculations on a light water system. From these calculations it was determined that the ratio of the average power density to the power density at the wall of the pressure vessel ranged between 7 and 9 for the nine cores calculated. It was felt that these values would also apply to the heavy water reactor systems since the shape of the flux plots should not differ greatly from those calculated. From the meager in-pile corrosion data (see Sec. 2.3.5), an allowable power density at the vessel wall of 6 kw/l was chosen. With this power density at the wall, and choosing 7 as the ratio of average to minimum power density, the core diameter is fixed by the total power requirement as 5.04 ft for both the light and heavy water systems. The diameter was arbitrarily reduced, however, to 5 ft which results in an average power density of 43 kw/l. It is interesting to note that this diameter is about the optimum size for both light and
5 ft DIAMETER SPHERICAL CORE
6 in. PRESSURE VESSEL THICKNESS
UO₂SO₄·H₂O SYSTEM (U²³⁵ CONC-13.7 g/l)
93 % U²³⁵
T = 20°C
kₐ = 1.035

Fig. 7 - Typical Three Group Flux Plot
heavy water systems as shown by the economic studies of Kasten and Claiborne.  

3.6 Solution Concentration

The concentration of uranium required for steady state operation of a 5-ft spherical core is shown as a function of average core temperature for the light water system by Fig. 8 and for the heavy water systems by Fig. 9. The concentrations were calculated for the clean condition from Eq. (3.2.1) using the concept of a reflector savings. The assumed 6-in. iron pressure vessel thickness effectively amounted to a reflector savings of about 7.5 cm for the light water system. A reflector savings of 15 cm was estimated for the heavy water systems from the work of Tobias. It is seen from the figures that the concentration of uranium in g/l increases with increasing temperature for the heavy water systems and decreases with increasing temperature for the light water system. It should be noted, however, that the concentration of uranium in g/kg of water increases in both systems with an increase in temperature.

An explanation of why the uranium concentration in g/l decreases with increasing temperature for the light water system can be given in terms of Eq. (3.2.1). Consider, for example, two 5-ft diameter spheres containing light water, one at 20°C and a density of 1 g/cm³ while the other is at 280°C and a density somewhat less than 1 g/cm³. The critical concentrations for these two systems are calculated to be 12.8 g/l for the 20°C reactor and 10.7 g/l for the 280°C reactor. These reactors are very lightly loaded, and it is observed from the calculations that, for the lower temperature case, about half the absorptions are in uranium and the other half are in hydrogen. For the higher temperature reactor, it is also observed that
Fig. 8 - $^{235}\text{U}$ Concentration as a Function of Temperature
Fig. 9 - $U^{235}$ Concentration as a Function of Temperature
both the fast and thermal non-leakage probabilities have decreased, whereas
the quantity $K_T = \sqrt{\Sigma_{\text{FT}} / \Sigma_{\text{AT}}}$ has increased due to the greater loss in hydrogen
absorptions (caused by a lower water density) over those in uranium. Since
the absorption cross sections are essentially $1/v$, there is no appreciable
effect due to temperature changes in the cross sections. The net effect
is that the loss of hydrogen absorptions is more important than the increased
leakage for this large light water reactor. For smaller reactors of this
type, the leakage would be more important and the critical concentration
in g/l would increase with increasing temperature. This is clearly shown
by Fig. 4 where it is noted that the crossover point is for a 3-ft diameter
reactor. It is of interest to note that the hand calculations, based on
Eq. (3.2.1), were confirmed by the three-group, two-region Oracle cal-
culations. This confirmation points out that the effect of the mathematical
model is not too important.

3.7 Effective Multiplication

The effective multiplication constant was computed by the three-group,
two-region Oracle code for the light water system. The results are shown
as a function of fuel concentration by Fig. 10. Fig. 11 gives the same
information obtained from hand calculations for the heavy water systems.

3.8 Temperature Coefficient of Reactivity

The temperature coefficient of reactivity is given as a function of
average core temperature by Fig. 12 for the light water system and Fig. 13
for the heavy water systems. They are strongly negative for each of the
three reactor systems. It is observed that the temperature coefficients
are about the same for the 20% and the 93% enrichments in the heavy water
systems.
Fig. 10 - Effective Multiplication vs $U^{235}$ Concentration
Fig. 11 - Effective Multiplication vs $^{235}\text{U}$ Concentration

5 ft DIAMETER SPHERICAL CORE
6 in. PRESSURE VESSEL THICKNESS
$\text{UO}_2\text{SO}_4 - \text{D}_2\text{O}$ SYSTEM

$20^\circ\text{C}$

$280^\circ\text{C}$

93%

20%
Fig. 12 - Temperature Coefficient of Reactivity for UO₂SO₄-H₂O System
Fig. 13 - Temperature Coefficient of Reactivity for UO$_2$SO$_4$ - D$_2$O System (5-ft Spherical Core)
3.9 Fuel Burnout

The $^{235}$U burnout at full power is about 95 g/day for each of the three systems. From the daily burnout, the loop volume, and the information contained in Figs. 8 and 9, it was calculated that the change in the average temperature is about one half degree centigrade per day for each of the reactor systems. Since the temperature decrease per day is so small, it appears practical to absorb the burnout by a drop in the average temperature. In this manner it would be possible to refuel the reactor intermittently.

3.10 Long Time Operation

Uranium concentrations as a function of reactor operation time for all three systems operating at full power are shown on Figs. 14 and 15.

The calculations were performed using one group of fission products based upon the work of Blomeke and Todd \(^{(21)}\), and assuming no removal of fission products. The calculations assumed a reflector savings for the light water system of 7.5 cm and a bare sphere for the heavy water systems. All parameters used were those for a reactor temperature of 280°C.

The build-up of plutonium for the 20% enriched heavy water system was ignored; however a calculation did show that this assumption resulted in an error of only 10% for the $^{235}$U criticality concentration for an operating time of 400 days.

The initial rise in the uranium concentration is caused by xenon saturation. The curves continue to rise because of the buildup of fission products, corrosion products and heavier isotopes of uranium.

3.11 Summary of Nuclear Calculations

Summarized in Table V are the data obtained for a 5-ft diameter core. The light water system is based on a reflector savings of 7.5 cm, whereas
5 ft DIAMETER SPHERICAL CORE
6 in. PRESSURE VESSEL THICKNESS
UO$_2$SO$_4$ - H$_2$O SYSTEM (93 % U$^{235}$)
T = 280°C

U = U$^{235}$ + U$^{236}$ + U$^{238}$

U$^{235}$

INITIAL CRITICAL CONDITION

Fig. 14 - Uranium Concentration vs Time (43 kw/l Average Power Density, No Fission Product Removal)
Fig. 15 - Uranium Concentration vs Time (43 kw/l Average Power Density, No Fission Product Removal)
the heavy water systems are based on a bare sphere calculation. Cold-clean values are for $20^\circ$C and no fission product poisons. Hot-clean values are for $280^\circ$C and no fission product poisons.

**TABLE V. SUMMARY FOR 5-ft CORE**

<table>
<thead>
<tr>
<th></th>
<th>$\text{UO}_2\text{SO}_4\cdot\text{H}_2\text{O}$ System</th>
<th>$\text{UO}_2\text{SO}_4\cdot\text{D}_2\text{O}$ Systems</th>
</tr>
</thead>
<tbody>
<tr>
<td>$^{235}\text{U}$ Enrichment, %</td>
<td>93</td>
<td>93</td>
</tr>
<tr>
<td>$^{235}\text{U}$ Critical Conc Cold-clean, g/l</td>
<td>12.8</td>
<td>1.3</td>
</tr>
<tr>
<td>$^{235}\text{U}$ Critical Conc Hot-clean, g/l</td>
<td>10.7</td>
<td>3.7</td>
</tr>
<tr>
<td>$^{235}\text{U}$ Critical Mass at $20^\circ$C, kg</td>
<td>23.0</td>
<td>2.4</td>
</tr>
<tr>
<td>$^{235}\text{U}$ Critical Mass at $280^\circ$C, kg</td>
<td>19.2</td>
<td>6.8</td>
</tr>
<tr>
<td>Temp Coeff of ReactIVITY at $280^\circ$C, $^\circ$C$^{-1}$</td>
<td>$-4.2 \times 10^{-4}$</td>
<td>$-5.2 \times 10^{-3}$</td>
</tr>
<tr>
<td>Average Power Density, kw/l</td>
<td>43</td>
<td>43</td>
</tr>
<tr>
<td>Average Thermal Neutron Flux, n/cm$^2$-sec</td>
<td>$1.2 \times 10^{14}$</td>
<td>$7.5 \times 10^{14}$</td>
</tr>
</tbody>
</table>
CHAPTER 4 SAFETY, STABILITY, AND CONTROL

4.1 Introduction

The safety and stability studies have been based on a set of coupled differential equations which were developed by Kasten\(^{22,23}\) to describe the Homogeneous Reactor Test (HRT). The set of equations are referred to as the equations of motion, and they relate changes in reactivity to the physical system. One of these equations is of interest here, namely

\[ k_e = 1 + \Delta + bt + \frac{dk_e}{d\rho} (\rho - \rho_0), \quad (4.1.1) \]

where

- \( k_e \) = effective multiplication constant
- \( \Delta \) = instantaneous reactivity addition
- \( b \) = linear rate of reactivity addition
- \( t \) = time
- \( \frac{dk_e}{d\rho} \) = density coefficient of reactivity
- \( \rho \) = average density of the fuel solution
- \( \rho_0 \) = average density of the fuel solution at initial conditions.

The important point shown by this equation is that changes which affect the reactor from a nuclear standpoint must be resolved into an instantaneous reactivity addition and/or a linear rate of reactivity addition.

The equations of motion were solved using the Oracle for a large number of cases by Kasten to study the safety of the HRT. From a safety standpoint, the maximum pressure rise in the core is the limiting factor. An approximate expression, developed by Kasten, can be used to determine the maximum pressure rise in the core. Data obtained using the approximate expression were compared
to the Oracle calculations (which were considered correct). For pressure rises up to 500 psi, the results were found to agree quite well; for higher pressure rises, the results were found to be pessimistic. However, the approximate expression for maximum pressure rise was used for studying the safety of the reactor described herein (Homogeneous Burner), even though a pressure rise of 1000 psi was of interest.

The various equations and graphs needed to calculate and relate maximum pressure rise to reactivity changes are given in Appendix A. The safety and stability studies for the Homogeneous Burner were made for a light water system using fully enriched uranyl sulfate.

4.2 Determination of System Parameters

The various nuclear parameters required for the stability and safety calculations were determined by using the simple critical equation for a bare reactor. These calculations were done more or less independently of the reactor physics calculations of Chap. 3. Those calculations did not provide a coefficient relating the change of reactivity to the change of fluid density at constant temperature. Thus, the use of the critical equation provided a relatively easy means to obtain a consistent set of nuclear parameters. Throughout the calculations the fuel solution was considered to be pure $H_2O$ except for those quantities where the presence of uranium was of importance. This is a reasonable assumption since the fuel solution is very dilute (10.7 g/l at 280°C). The uranium in the fuel solution was considered to be pure $^{235}U$ for calculational purposes. The various numerical values used in this study can be found in Appendix A.

4.3 The Maximum Permissible Rate of Reactivity Addition

It was felt that a reasonable pressure rise for safety considerations
would be 1000 psi. This pressure rise, when added to the operating pressure of 1750 psia, is below the estimated rupture strength of the reactor vessel and does not, as the results will show, impose any severe operating restrictions.

The results of the maximum pressure rise calculations are shown by Fig. 16. Curve 1 represents the reactor as designed, while curves 2 and 3 show the changes expected due to a change in the number of velocity heads lost between the core and pressurizer. Curve 4 shows the effect of reducing the prompt neutron lifetime by 10%, which can be expected as the poisons buildup in the reactor system. Curve 5 shows the effect of changing the surge pipe (the connection between the reactor system and the pressurizer) from a 10 in. pipe to an 8 in. pipe. For the reactor as designed, $m_e$, the equivalent instantaneous prompt reactivity addition divided by the prompt neutron lifetime, was found to be 35 sec$^{-1}$ for a 1000 psi maximum pressure rise.

The quantity $m_e$ is not particularly important as such; however it provides the connection between the maximum pressure rise and the rate of increase of reactivity. The relation between initial power and rate of reactivity addition, for a constant $m_e$ and an initial $k_e$ of unity, is plotted in Fig. 17. The figure shows that the greater the initial power, the higher the permissible rate of reactivity addition. However, it raises the question as to what minimum initial power to use.

At this point the neutron source must be considered. When evaluating the safety of aqueous homogeneous reactor systems, the power at the time the reactor goes prompt critical is all important since changes in the system before this time are small. If the reactor is supplied with a source of $10^7$ neutrons/sec near the center of the core, the power level due to the source
Fig. 16 - Maximum Pressure Rise vs Reactivity Addition

\[
m_e = \frac{\Delta k_{eqp}}{l} \quad \text{(sec}^{-1})
\]
Fig. 17 - Rate of Reactivity Addition vs Initial Power

- $m_e = 35 \text{ sec}^{-1}$
- $m_e = 25 \text{ sec}^{-1}$
- $k_e(0) = 1$
neutrons is about $1.3 \times 10^{-4}$ watts with $k_e = 0.5$. If reactivity is added at the rate of $0.004 \Delta k_e/\text{sec}$, the power level rises to about $5 \times 10^{-2}$ watts by the time the reactor is prompt critical. This corresponds to an $m_e$ of approximately $35 \text{ sec}^{-1}$. It can be shown that a value of $k_e(0) = 0.5$ provides the worst condition for any given source.

The maximum permissible rate of reactivity addition for this reactor was found to be $0.004 \Delta k_e/\text{sec}$ based on the worst possible condition in the reactor.

4.4 Safety Considerations

The maximum rate of reactivity addition for a 1000 psi pressure rise has been established above. It now becomes necessary to relate the reactivity changes to reactor conditions.

Positive reactivity can be added to this reactor either by lowering the average core temperature or by increasing the fuel concentration in the core. It, therefore, becomes necessary to investigate the various possibilities by which these additions can occur. Positive additions are the only consideration, as negative additions will tend to shut the reactor down. It does not seem possible to have an instantaneous addition of reactivity in the present reactor design.

Attention is now directed to several cases which are believed to represent the credible safety problems.

Case I

The fuel solution is at the critical concentration, corresponding to a $280^\circ\text{C}$ average core temperature. The circulating pumps are suddenly stopped and fuel circulation ceases. The fuel temperature rises to $300^\circ\text{C}$ due to heating by decay heat. It is further postulated that the fuel solution in the heat exchangers is rapidly cooled to $180^\circ\text{C}$. Then the circulating pumps are started.
Under the above conditions, the rate of reactivity addition is calculated to be $0.02 \Delta k_e/\text{sec}$ when the reactor goes critical. This is based on the temperature coefficient of reactivity, and an average core temperature change which assumes no mixing of the fuel solution in the core. Under these conditions, reactivity is being introduced at a dangerous rate.

It is seen that the initial circulation rate must be limited to a value which, in turn, will limit the maximum core pressure rise. The reduced circulation rate can be readily obtained by reversing the direction of rotation of the circulating pumps on startup. The resulting reduced flow is maintained for a predetermined period to avoid the possibility of an excessive core pressure rise.

It is estimated that, for the Homogeneous Burner, a single pump running backwards will provide 20% of the normal flow. This will reduce the reactivity addition to a safe value. Thus, the control equipment will be designed to provide reversed rotation single pump initiation of fuel circulation.

Note that venting the steam drum to atmosphere could reduce the temperature of the fuel solution in the heat exchanger to something well below $180^\circ\text{C}$. Thus, a minimum shellside temperature of $180^\circ\text{C}$ will be required before the loop circulating pumps can be started again.

**Case II**

This case considers the effect on the operating reactor of a decrease in fuel solution temperature caused by a decrease in the steam temperature. Since the temperature coefficient of the reactor is $-5.1 \times 10^{-4} \Delta k_e/k_e -^\circ\text{C}$ (at $280^\circ\text{C}$), the maximum permissible rate of temperature decrease is about $8^\circ\text{C}/\text{sec}$. When the fuel solution temperature decreases at a rate greater than $4^\circ\text{C}/\text{sec}$, the circulating pumps will be stopped.
Case III

For this case, consideration was given to the ways in which the $^{235}$U concentration in the core could be increased. The concentration coefficient (at constant temperature), $\frac{\partial k_e}{\partial C}$, was calculated to be 0.46, which indicated that the reactor is quite sensitive to concentration changes.

A study of the reactor system shows that the surge pipe is filled with fuel solution with a $^{235}$U concentration of approximately 5% more than the main circulating system. There does not appear to be any reasonable manner by which this more concentrated solution can be introduced into the core in "large" quantities. However, this question does point out that certain design aspects of the reactor could be studied in greater detail. These will be discussed later.

The effect of a "slug" of concentrated fuel solution on reactivity was made using first order perturbation theory. If the "slug" was 10% more concentrated than the fuel solution, the maximum "slug" volume, which can be tolerated was about $3 \text{ ft}^3$. It was assumed that the "slug" entered the reactor at the center and expanded radially. This calculation has been included to try to give a feeling for the size of "slug" that might be tolerated.

The fuel feed pump has been sized, on the basis of a reasonable startup time, as 1.5 gpm. In order to avoid the possibility of building up a "slug" of high concentration fuel solution in the loop, interlocks will be provided to prevent the operation of the fuel feed pumps when both circulating pumps are stopped. The feed line connection on the dump tanks is located such that a minimum of 20 $\text{ft}^3$ of solution is in the dump tanks at all times. In this manner it is possible to limit the maximum $^{235}$U concentration of the solution being pumped into the circulating system.
One possible method of introducing a more concentrated fuel solution into the circulating system would be to open the dump valve for a short period of time. This would permit the introduction of a more concentrated solution into the system from the surge pipe. It is not clear that the reactor would respond favorably under such a situation. Thus, the dump valve will be so designed that when it has been opened, special steps will be required to close it again.

It was stated above that special steps would be required to close the dump valve once it has been opened. Some nebulous questions arise when one considers the state of the reactor core while dumping the system. For instance, vaporization and the flow of concentrated solution into the core from the surge pipe and pressurizer should give a more concentrated solution in the core which results in a pressure rise. However, the condition of the system during the dump would probably make this a tolerable situation.

Normal letdown of solution from the system is taken care of by a small letdown line connected to the surge pipe.

4.5 Stability

The safety problems discussed above were concerned with large changes of reactivity in the system. The stability problem is concerned with small changes in reactivity.

The stability studies of Kasten \(^{23}\) were based on (1) linearizing the equations of motion, (2) making suitable approximations based on the type of stability in question, and (3) using the nature of the roots of the characteristic equation as the stability criteria. This system has been adopted to study the stability of the reactor described herein.
Nuclear stability is concerned with relatively high frequency oscillations set up between the core and pressurizer. The stability criteria are given in Fig. 18. A reactor is stable for this type oscillation provided the operating point is to the left of the curve shown in the figure. It was found that this reactor is stable for power levels up to 220 Mw.

Circulating fuel reactors, such as the type described, are found to be stable to load demand fluctuations by the method of analysis indicated above.

4.6 Introduction to Instrumentation and Control

The Homogeneous Burner Reactor System is similar in many respects to "conventional" systems found in modern chemical plants. To instrument and control the system, it is desired to measure temperature, pressure, flow, and liquid level. The reactor system differs from the "conventional" in that the sensing devices must function in very high radiation fields and must be leak-tight. Further, the reliability of the device should be greatly improved since long maintenance-free life is required. Repair or replacement is difficult and costly. It may also be well to recall the system conditions, a corrosive solution at 1750 psi and temperatures up to 300°C.

The safety study showed that the Homogeneous Burner is adversely affected by relatively few things; even in those cases, millisecond corrective measures are not required. Thus, the control system can be relatively simple with most of the safety features provided in the form of interlocks. Further, the need for automatic control loops is minimized since it is an inherent characteristic of the reactor to follow load changes.

4.7 Control

The description of the controls will be limited to those things which are of particular importance to the operation and safety of the reactor system.
Fig. 18 - Stability Criteria for Homogeneous Burner Reactor
4.7.1 Circulating Pumps. By means of a selector switch, either pump may be started first. The one selected will be started in the reverse direction. After one minute of reverse operation and with a 1000 gpm flow rate the operator may initiate operation in the normal direction.

A minimum shell side heat exchanger temperature of 180°C is required for operation of the circulating pumps with uranium in the solution. A decrease in fuel temperature of 4°C/sec will stop the circulating pumps. This condition could be caused by a decrease in steam temperature and pressure. A decrease of pressure in the steam drum will provide the controlling signal.

4.7.2 Fuel Make-Up Pump. Operation of this pump will be contingent upon (1) the normal operation of either circulating pump, (2) a minimum loop flow rate of 5000 gpm, and (3) an average core temperature less than 285°C. The pump will be controlled by the operator.

4.7.3 Water Dilution Pump. Operation of this pump will be initiated when the core outlet temperature reaches 305°C.

4.7.4 Dump Valve. As has been previously mentioned, the operation of the dump valve will be so arranged that once it has opened, special steps will be required to close it.

Three types of dump operations are provided for:

1) A normal controlled draining of the system.

2) An emergency dump operation with a two minute time delay. This type operation could be initiated by excessive radiation in the steam or a core outlet temperature of 312°C. The two minute time delay is provided to permit recombination of the radiolytic gases. This also gives the operator a chance to block the dump if he should so desire. Note that an alarm sounds when the two minute timer is energized.
3) An emergency dump without time delay. This could be initiated by a 2800 psi system pressure. This type dump is also available to the operator.

Note that any emergency dump operation starts maximum flow of cooling water to the reflux condenser on the dump tanks.

4.7.5 Pressurizer. The pressure of the pressurizing fluid is automatically controlled by releasing $O_2$ and $H_2O$ vapor or by adding $O_2$, as required.

The liquid level in the pressurizer will normally be manually controlled. An exception to this will be an extra high level, in which case the letdown valve will be level controlled.

4.7.6 Excess Radiation in the Steam. Radiation monitors in the main steam line will function to close valves in the steam and feed-water lines in the event of leakage of fuel solution into the heat exchanger.

Each steam generator will be provided with its own radiation monitor to facilitate the location of the defective unit.

4.7.7 Bypass Facilities for Interlocks and Controls. Certain of the interlocks and control features will be provided with bypass and/or blocking facilities. These will be arranged for easy access to the operator and in such a manner that any change will be readily apparent.

4.8 Instrumentation

The instrumentation presents no special problems except as indicated above in connection with sensing devices. In addition to indicating and recording type instruments, an annunciator system will be used to call the operator's attention to abnormal system conditions.

Nuclear instruments will indicate and record neutron flux for all power levels as well as providing the reactor period. This information will not
be used for automatic control of the reactor.

Radiation detection instruments will be provided for the protection of personnel and equipment. Except as indicated in Section 4.7.6, these devices will provide only audible and visual alarms.

4.9 Conclusions on Safety, Stability, and Control

It is concluded that the Homogeneous Burner Reactor described here will be stable and can be operated safely; however it should be noted that safe operation for all conditions is dependent on the reactor control system. It is felt that the calculational methods used in this analysis of safety and stability gave pessimistic results in all cases.

Although the above analysis was based on a light water uranyl sulfate solution, it appears that the safety and stability of a heavy water system would present no obstacles. Similar calculations could be made to establish the operating limitations of such a system.
CHAPTER 5 PROCESS DESIGN

5.1 Flowsheet

A schematic flowsheet of the reactor system is shown by Fig. 19. The HRT flowsheet and design data were used as a guide in the selection of the flowsheet proposed here. About 10,000 gpm of fuel solution at 260°C enters the 5-ft diameter core where 80 MW of heat are produced. The solution, at 1750 psia, is heated to 290°C before leaving the core by way of the concentric outlet. The necessary mixing of the solution in the core is obtained by use of a concentric inlet and outlet located at the north pole of the spherical reactor vessel.

The fuel solution (UO₂SO₄ - H₂O) leaving the core is divided and half goes to each of two 5000-gpm canned rotor circulating pumps which develop about 75 ft of head at full power. The pumps, each equipped with a check valve, discharge to a common header which, in turn, serves four 20 MW heat exchangers. The four exchangers produce at full power a total of 320,000 lb/hr of saturated steam at 515 psia and 470°F. The necessary steam separation and drying equipment are contained in a horizontal steam drum located above the vertical heat exchangers. The fuel solution leaves the exchangers and returns to the core to complete the primary circuit. With the proposed arrangement the system is capable of operating at about 80% of full load with one pump out of service by taking a larger temperature drop across the heat exchangers (294 to 256°C). In the event both pumps are out of commission, natural recirculation is more than sufficient to remove the decay heat.

The system is pressurized by bleeding in oxygen from a 2500-psia oxygen receiver to maintain a total pressure of 1750 psia. Since the normal operating
Figure 19 Homogeneous Burner Reactor Flowsheet
temperature of the pressurizer is about 300°C, the oxygen overpressure is approximately 500 psi.

Purge water for the circulating pumps is obtained by circulating the pressurizer gas and water vapor mixture through a purge condenser. Gas circulation through the condenser is obtained by the use of a jet pump. The prime mover for the jet pump is a small liquid stream (20 gpm) from the discharge side of the circulating pump. This liquid passes through the pressurizer and is returned to the suction side of the circulating pump through a large diameter standpipe. The standpipe was sized to limit the pressure rise expected when liquid is removed from the core caused by sudden changes in reactivity or load.

The pressurizer system, in addition to its function of acting as a surge volume and a source of purge water, provides a means of controlling the fuel concentration. The approximately 95 g/day fuel burnup, through the negative temperature coefficient of reactivity, lowers the average core temperature about 1/2°C/day. This suggests refueling every few days. In this manner, the service life of the fuel feed pump is greatly extended. The solution is concentrated by allowing some of the purge water to be bypassed to the condensate storage tanks. See Appendix D for pressurizer heat balance.

The condensate storage tank provides sufficient holdup to dilute the solution for concentration control and normal shutdowns. The tank is fed by either the bypass from the purge condenser or a feed pump connected to the secondary recombiner-condenser.

5.2 Gas Pressurization (28, 29)

Although considerable development was performed on a steam pressurization system for this type reactor, it is felt that gas pressurization could
offer some advantages, especially if the pressurizing gas were oxygen. The 500-psi partial pressure of oxygen in the pressurizer saturates the solution and is more than sufficient to inhibit corrosion and to prevent uranium precipitation in stagnant lines where oxygen has been depleted by recombination in the solution.

Since it is necessary to relieve the pressure in the system as rapidly as possible during a dump operation, it appears that oxygen facilitates this operation. When the reflux condenser on the dump tanks has condensed all the water vapor, there remains a residual gas pressure of about 45 psi in the system. If an inert gas such as helium were used, it would have to be pumped out of the system to lower the pressure. This will necessitate the use and development of a remotely maintained multi-stage compressor since it would also pump fission product gases.

While it is possible to dispose of the excess oxygen by other chemical means (i.e. a copper bed), it is proposed to burn off the excess oxygen with hydrogen in a recombiner and to pass the remaining fission gases through an activated charcoal bed. The oxygen system is essentially shown in Fig. 19. Oxygen is supplied to a standard oxygen compressor (0.8 cfm capacity) where it is compressed to 2500 psi and stored in an oxygen receiver. From this receiver it is possible to pressurize both the high pressure system and also to sweep the dump tanks. The latter provision makes it possible to sweep out residual radioactive gases before opening the primary loop for maintenance.

5.3 Internal Recombination of Radiolytic Gases

Sufficient CuSO\(_4\) (0.02 m) has been added to the solution to obtain 100% recombination of the hydrogen and oxygen formed by radiolytic decomposition
of water while operating at full power. While it would be advisable to operate with a minimum of CuSO₄ for solution stability (see Section 2.2.2), sufficient copper must be present to prevent the accumulation of explosive mixtures of H₂ and O₂ in the vapor space above the pressurizer and to prevent the formation of gas bubbles in the core.

In the determination of the required amount of CuSO₄, an average temperature of 275°C for the loop was assumed along with the saturation value for dissolved hydrogen at the core exit. Although the activity of the CuSO₄ changes with temperature, it was found in doing a study for the HRT(5) that these approximations gave reasonable values. The calculations for this system are given in Appendix C.

With no recombination of the gases, the production rate of H₂ is 259 liters H₂/sec at STP or 0.0255 lb-mol H₂/sec. With a fuel solution inventory of 750 lb-mols, the average production rate is 3.40 x 10⁻⁵ lb-mol H₂ per sec per lb-mol of solution. With a copper concentration of 0.02 m, the partial pressure of hydrogen at the core exit is determined to be 74 psi, or a concentration of 3.35 x 10⁻⁴ lb-mol H₂ per lb-mol of solution.

With the specified amount of copper in the system, the recombination rate of the dissolved gases is very rapid, with about 95% recombined in a matter of 10-15 seconds at 300°C. With this rate of recombination and the long residence time (585 sec) of the fuel in the pressurizer, the concentration of H₂ in the pressurizer vapor space is negligible. This is fortuitous as the data with regard to the explosive limits of the H₂-O₂-H₂O vapor system are conflicting(30). Since explosive limits of 1 to 18 mol percent H₂ are reported(30), it is intended to keep the H₂ concentration below 1% for this reactor.

Since the residence time in the pressurizer is very long, the fuel
solution returning to the primary loop will be saturated in oxygen and no dissolved hydrogen. It is important to insure that this solution will not become supersaturated with oxygen upon mixing with the primary loop. The system pressure is maintained at 1750 psia by an overpressure of oxygen. Since the pressurizer operates at 300°C, the partial pressure of the water vapor is 1250 psi, hence the partial pressure of oxygen is 500 psi. The solution returns to the core outlet stream which is at a temperature of 290°C and a partial pressure of water vapor of 1080 psi. Since the partial pressure of H₂ in the core outlet stream is 70 psi the oxygen partial pressure is 600 psi. From Fig. 20 it is seen that the solubility of oxygen at 300°C and 500 psi partial pressure is less than that at 290°C and 600 psi, hence the solution will not become supersaturated in oxygen and gas bubbles will not be formed. Similarly, there is no danger of gas coming out of solution upon cooling the liquid in the exchangers. Since the total pressure remains essentially constant, the partial pressure of oxygen at the heat exchanger exit can be increased to about 1045 psi. From Fig. 20 it is seen that the solution is not saturated with oxygen and bubbles will not form.

5.4 Low Pressure Dump Tank System

The low pressure system design conditions have been determined on the basis of reactor startup and shutdown operations. The reactor dump tanks are interconnected by means of slanted evaporator legs which are connected to the bottom of these tanks. The purpose of the evaporators is to provide adequate circulation in the tanks and to evaporate water for reactor start-up; also attached to the dump tank is a reflux condenser whose primary function is to reduce the system pressure during a dump.
Fig. 20 - Solubility of Oxygen in H\textsubscript{2}O
To relieve the oxygen overpressure remaining after a dump, the excess oxygen will be burned with hydrogen in the secondary recombiner-condenser system. The recombiners will be operated with excess hydrogen. The unused hydrogen, along with the fission gases, will be passed through an activated charcoal bed and to the atmosphere.

5.5 Startup and Shutdown

The recommended startup procedure is to fill the system with pure condensate, evaporated from the dump tanks, and preheat the system to a temperature of 200°C. During this period the main circulating pumps will be run to provide circulation in the system. Also oxygen will be fed into the pressurizer to maintain an overpressure of at least 500 psi. The necessary heat for startup will be obtained by supplying steam to the shellside of the main heat exchangers. A package oil fired boiler, rated at 2500 lb/hr, delivers steam at 400°F for this purpose.

The change in temperature should be limited to 50°F/hr to protect the heavy walled equipment in the primary loop system from excessive thermal stresses. The excess water in the loop, due to expansion upon heating, will be bypassed to the condensate storage tank. In this manner the time required for startup can be reduced considerably since there will be less dilution of the remaining fuel solution in the dump tanks.

With the system at 200°C, concentrated fuel solution is fed into the system from the dump tanks. Excess water will be bypassed to the condensate storage tank and the reactor brought to critical. Heat losses from the system will bring the power level to approximately 400 kw. Additional uranium will be added to the system until an average core temperature of 275°C is reached.
Normal shutdown of the reactor is accomplished by reversing the startup procedure. The reactor will be brought to essentially no load condition with only decay heat being removed. The fuel circulation is continued while dilution water is added from the condensate storage tanks. The temperature of the system is slowly lowered to about 150°C in order to reduce the service on the dump valve and to prevent excessive thermal stresses on the heavy wall equipment.

The primary loop can be dumped from operating temperature and pressure. However, under these conditions it is advisable to circulate the system at temperature for several minutes to allow complete recombination of the radiolytic gases. The pressure rise in the dump tank will be relieved by condensing the flashing steam and burning off the excess oxygen.

5.6 Solids Removal System

A schematic flowsheet of the hydroclone solids removal system is shown in Fig. 21. A small stream (3.2 gpm) from the pump discharge is passed through a micrometallic filter in order to trap any large size particles which would clog the hydroclones. The stream then flows into a bank of four-0.4 in. hydroclones connected in parallel. The solids are separated by the centrifugal action of the hydroclones and collected in a 25-l underflow pot for further chemical treatment. The clarified overflow solution from the hydroclones is returned to the primary loop by a jet pump as shown by the flowsheet. In addition, the micrometallic filter is properly valved for backwashing the filter with solution. This backwash fluid is retained for further chemical treatment.

Assuming a separation efficiency of 50% for this system, it is possible to operate the hydroclones for 8 hr/day and maintain a solids concentration
Fig. 21 - Solids Removal Flowsheet
in the loop of 150 ppm. In order to prevent clogging of the underflow drain lines, a maximum solids concentration of 40-50 g/l in the pots is recommended. Based on a corrosion rate of 1.0 mil/yr approximately 500 g/day of solids will be collected in the pots. This requires that about 10 l of concentrated solution be treated each day. However, for the 1.0 mil/yr corrosion rate, it is necessary to remove about 30 l/day for treatment in order to limit the corrosion product nickel concentration to about 0.03 m. With this removal rate there should be no danger of the hydroclones plugging.

Since the solubilities of most of the fission products decrease with increasing temperature, it is necessary to maintain the underflow pots at about 300°C. At this temperature all the solids separated by the hydroclones remain suspended in the solution. Also there is no danger of forming two liquid phases. The underflow pots are maintained at this temperature by transferring the decay heat to a pressurized cooling water jacket.

If the corrosion rate should prove to be less than that assumed above as a result of the conditioning runs prior to reactor operation, the processing cycle could be extended or, perhaps, the removal of nickel would be unnecessary. If this were the case, it is recommended that the solids be allowed to buildup in the underflow pots. When the underflow becomes saturated, the unit could be removed from the line, and replaced by a new unit. The amount of uranium contained in the pots would not be worth the expense of reprocessing.

While no experimental data have been obtained as to the maximum concentration it is possible to attain in the underflow pots, experience with these systems has shown that the solids will settle out. It is believed that a concentration of 500 g/l can be attained in the underflow pot.
5.7 Hydrogen Peroxide Uranium Recovery System

As was pointed out earlier, for an expected corrosion rate of 1.0 mil/yr, the solution will become saturated with corrosion product nickel. If this occurs the solution must be processed to remove the nickel and recover the uranium. A method for doing this is now under development at ORNL. It is known as the Hydrogen Peroxide Precipitation Process\(^9\) and is shown schematically by Fig. 22. Basically, the system takes advantage of the high solubility of the fission and corrosion products at low temperature and the fact that hydrogen peroxide will precipitate uranium as \(\text{UO}_4^-\) from \(\text{UO}_2\text{SO}_4\) solutions.

The feed for this batch process is obtained from the hydroclone underflow pots. The solution is cooled to redissolve as many of the solids as practical in the separation process. Contained in the solution from the underflow pots are:

1) Soluble uranium as \(\text{UO}_2\text{SO}_4\)
2) Insoluble uranium imbedded in the fission and corrosion products
3) Soluble and insoluble fission products
4) Soluble and insoluble corrosion products.

The cooled solution is filtered or centrifuged to remove the suspended solids. The supernatant liquid is recovered and refrigerated. Hydrogen peroxide is added to precipitate the uranium. The solution is then decanted, and the liquid is sent to the waste disposal system. The remaining \(\text{UO}_4^-\) precipitate is washed, dissolved, and converted to \(\text{UO}_2\text{SO}_4\) by adding dilute \(\text{H}_2\text{SO}_4\), before returning it to the dump tanks for future use. The uranium recovery is 95 to 99%, depending on the number of washes and the number of times the solution is recycled.
30 liters/day
UO$_2$SO$_4$ SOLUTION
(STREAM CONTAINS
SOLUBLE AND INSOLUBLE
CORROSION AND FISSION
PRODUCTS)

H$_2$O WASH

DISPOSABLE FILTER

Fe, Cr AND INSOLUBLE
F.P.'s REMOVED

H$_2$O$_2$ ADDITION
H$_2$SO$_4$ ADDITION

REFRIGERANT

DECANTED SOLUTION
TO WASTE DISPOSAL
CONTAINS Ni, Cs, Sm
AND RARE EARTHS

UO$_2$SO$_4$
TO DUMP TANKS

Fig. 22 - Hydrogen Peroxide Uranium Recovery System
CHAPTER 6  STEAM CYCLE

6.1 Full Load Conditions

The steam cycle selected for this reactor study may be described by Figs. 23 and 24. The numbered points in the following text correspond to the designated points in the above mentioned figures.

At full load 320,000 lb/hr. of steam at 515 psia and 470°F with a moisture content of less than 1% leaves the steam drum (point 1) for the high pressure turbine. This particular turbine operates at 3600 rpm and has a feedwater bleed (point 2) of 28,800 lb/hr of steam at 200 psia and 5.7% moisture. The high pressure turbine exhausts at 50 psia and 11.3% moisture (point 3) to a moisture separator which channels 28,800 lb/hr of liquid along with 28,800 lb/hr of steam at 45 psia and 99.9% quality (point 4) to the deaerating heater.

After the separator a 3600-rpm low pressure double flow turbine is utilized. This turbine has a feedwater bleed of 22,400 lb/hr of steam at 8 psia and 7.2% moisture (point 5) which is directed to the third feedwater heater. The low pressure turbine exhausts at 0.8 psia and 12% moisture (point 6) to the condenser (point 6'). The condenser requires 39,000 gpm of 80°F cooling water. The temperature rise of the cooling water while passing through the condenser is 10°F.

The over-all turbine and generating efficiency is calculated to be 28.5%, thus 22,800 gross kw are produced. It is estimated that the plant auxiliaries will not require more than 2,800 kw. That being the case, there will be about 20,000 kw net available at the bus bar. A more detailed analysis of the auxiliary power requirements cannot be performed for this study as the pumping requirements will depend very strongly on the source of cooling water.
Fig. 23 - Steam System Flowsheet
6.2 Partial Load Conditions

The steam conditions for partial loads for constant fuel concentration with both circulating pumps at normal speed are shown in Fig. 25. It is observed that the steam conditions go from 515 psia and 470°F at full load to 863 psia and 527°F at no load conditions. The steam conditions for constant fuel concentration and one circulating pump out of service are shown by Fig. 26. It is noted that the steam conditions are slightly lower than the design conditions at 100% load. However, temporary operation at the lower pressure would not harm the turbine.
BOTH FUEL CIRCULATING PUMPS AT CONSTANT SPEED
CONSTANT FUEL CONCENTRATION

Fig. 25 - Steam Conditions vs Turbine Load
ONE FUEL CIRCULATING PUMP OUT OF SERVICE
CONSTANT FUEL CONCENTRATION

Fig. 26 - Steam Conditions vs Turbine Load
CHAPTER 7 MECHANICAL DESIGN OF REACTOR COMPONENTS

7.1 General Description of System

The philosophy followed in the design and layout of the reactor plant is one of reliability and simplicity of operation with economic power not being a prime consideration. The choice of components has been to favor those which have been demonstrated in reactor, or similar, service. In this manner it is felt that a minimum of component development is required for this reactor.

A comprehensive economic study of the many design possibilities for the reactor system was not undertaken. At those points in the design where it was not clear what the economic choice would be, an arbitrary choice was made which appeared to be the best compromise between reliability and economic considerations. Typical examples were the selection to two circulating fuel pumps instead of one, and the selection of four small heat exchangers instead of one or two large exchangers.

The power station is shown in Fig. 27. It is noted that a containment vessel, surrounding the reactor system, and being adjacent to the control room, has its lower portion below the ground level for convenience in removing and replacing equipment within the vessel. Also, a considerable shielding savings is anticipated by lowering the reactor system below grade. The reactor pressure vessel is placed within the containment vessel on the side nearest the control room so that an instrument thimble may penetrate the shield and containment vessel. It is proposed that the control area consist of four levels with an instrument tunnel providing access for instrumentation to the containment vessel on each level.
Figure 27  General Plant Layout
A symmetrical layout, as shown in Fig. 28, has been chosen as the most promising for this reactor. It is noted that the layout shows only the main components and connecting piping. A sectional elevation of the proposed system is shown in Fig. 29. It is noted in this figure that the reactor is located at the bottom of the containment vessel and all other components are located above, with the exception of dump tanks. This is to facilitate heat removal by natural convection in the event that both pumps fail. Vertical heat exchangers were chosen over horizontal ones in the belief that the vertical ones offered better prospects for remote removal.

From the results of an elementary flexibility analysis on the piping system, it is believed that the system will have to be designed so that some of the equipment is free to move when heated to operating temperature. The connecting piping, which has a large diameter and a very heavy wall thickness, is extremely stiff. A close coupled system such as shown by the layout will present some difficult design problems.

The concept of a unit shield was followed in laying out the system. It is believed that greater flexibility in remote maintenance is offered by this concept. The reactor vessel is enclosed in a unit shield in the lower portion of the containment vessel in an attempt to isolate this large radiation source from the remaining equipment. The pressurizer and oxygen receiver tank are located above the reactor vessel shielding. The circulating pumps are located in individual cells to facilitate maintenance and to reduce the radiation damage to the motor windings. The heat exchangers are located in the two cells beneath the horizontal steam drum. The walls of the unit shields must not extend to the roof plugs since sufficient area must be available for communication between cells in the event of a vessel
Figure 28 Reactor System Plan View
Figure 29 Reactor System Elevation
rupture. Two large cells are available for the many smaller components necessary in the design of the reactor power plant. Sufficient openings have to be left in the roof plugs to permit the use of the upper portion of the containment vessel for an expansion volume.

It is proposed that all equipment, which is susceptible to failure, be located so that it is accessible for removal from above. The pump housings are welded in place. Repairs are made by removing the rotor and impeller from above. The fuel pipes are flanges to the heat exchangers while the steam risers and downcomers are welded in place. It is believed that they can be cut and rewelded by flooding the system below. The reactor vessel is not accessible from above as there appears to be no need in providing access to this item. Access to the containment vessel will not be permitted while the reactor is producing power. However, access, when permissible, is through air lock passages.

The building adjacent to the containment vessel houses all the supporting facilities. The turbine room is located as shown in Fig. 27. The turbine condenser and associated equipment are located beneath the turbine room. A twenty ton crane bay runs the entire length of the turbine room and extends out over the dump tank area, past the access door of the containment vessel, to the large storage pool beyond. Radioactive equipment is removed from the containment vessel on a dolly and then taken by the crane to the pool for storage.

The auxiliary boiler, used for heating the system on startup and for providing process steam, is housed in the area adjacent to the turbine room. It is anticipated that this boiler will be an oil fired high pressure package unit.
The dump tanks are located in a separate parallelepiped containment vessel as shown in Fig. 27. The vessel is accessible from above when necessary. A large interconnecting channel provides access from the main containment vessel to the dump tank storage area in order to use this volume for expansion in the event of a vessel failure.

Shielded sample lines are provided to a small hot cell located on one of the lower levels of the supporting facilities area. A small stream is circulated through the hot cell for samples and removal of solids and then returned to the reactor system.

A waste disposal system must be provided for concentrating and storing the radioactive waste from the reactor system. The necessary ventilating equipment must be provided so that the containment vessel can be operated at reduced pressure. If there is leakage, this will permit in-leakage of air instead of out-leakage of radioactive gases.

Space coolers must be provided within the reactor shield to maintain a reasonable ambient temperature. The entire system must be designed for frequent earthquakes of relatively severe magnitude. Extreme precautions must be taken in the design, fabrication, and inspection to give assurance that the system has the highest integrity.

The material in contact with fuel solutions must be the appropriate ASTM or SA specification for 18-8 Type-347 or Type-304 ELc SS. For items of equipment which are of sufficient size to permit the use of clad material, the back up material shall be SA 302 or SA 212 Gr. B. The entire system is designed for 2000 psi at 600°F with 0.1 in. corrosion allowance.

The main items of equipment are discussed in more detail in the following paragraphs.
7.2 Pressure Vessel

The proposed reactor pressure vessel design is shown in Fig. 30. The vessel, designed for 2000 psi pressure while operating at full power of 80 Mw, contains three equally spaced stainless steel thermal shields. Constructed of 18-8 Type-347 stainless clad carbon steel, the 6-ft ID pressure vessel is 3.3 in. thick of which 0.3 in. is the stainless steel clad. Each of the thermal shields are 1.187 in. thick with 0.187 in. being the corrosion allowance. The method of determining the vessel thickness and the number of thermal shields required is presented in Appendix B.

The vessel is designed to provide a concentric inlet and outlet so that a diffuser is not needed to distribute the flow\(^{(28)}\). The outlet annulus has two-12 in. Sch. 120 nozzles for directing the flow to each of the pumps. The inlet nozzle is 18 in. Sch. 120. At the bottom of the vessel, a 3 x 0.469 in. wall nozzle is provided. During normal operation some flow is permitted to enter through this nozzle for cooling the thermal shields. The coolant flow distribution between the thermal shields was not investigated. It is recognized that a design problem may exist at this point.

A 1/2-in. thick carbon steel vessel with cooling coils attached surrounds the pressure vessel, however this is not shown in the figure. During normal operation it is necessary to transfer about 5560 Btu/hr-ft\(^2\) from the pressure vessel to the thin carbon steel heat collector so that the minimum thermal stress condition exists. An investigation of the possible effects of a brittle fracture of the pressure vessel was not undertaken. If necessary, the heat collector can be designed to provide protection for this type failure.

The weight of the vessel is estimated to be 13 to 14 tons and the
Figure 30  Reactor Pressure Vessel
over-all height about 11 ft. The base material has been selected as Sa 302 in order to take advantage of its higher allowable stress at operating temperature. The vessel is to be constructed in accordance to the ASME Boiler and Pressure Vessel Code, Section VIII.

7.3 Heat Exchangers

Steam is produced in four stainless steel vertical heat exchangers which are coupled to a common horizontal type steam drum. The construction details of the heat exchangers are shown in Fig. 31. A recirculation ratio of 4:1 is employed in the natural circulation steam generation system.

While four exchangers are likely to have an initial cost greater than that of a single unit, it is felt that reliability and replacement are considerably enhanced by the somewhat arbitrary choice of four units. The individual units are of such size and simplicity that it appears feasible to stock an extra unit for replacement in the event one unit develops leaks. The replacement unit will be identical to the initial units except that diametrically opposed downcomer and riser nozzles are supplied so that it will not be necessary to have right and left hand replacements. The unused nozzles will be capped before installation.

The exchangers are of the straight through type with elliptical heads welded in place. Differential expansion is minimized by the use of an all stainless steel construction. The simplicity of this design is believed to offset the greater cost of clad type construction or U-bend exchangers with a pass partition. The fuel pipes are fitted with ring joint flanges which are accessible from above for remote removal from the system. The carbon steel downcomers and risers are welded in place. It appears feasible to cut these lines and reweld them either under water or above the water level
Fig. 31 - Heat Exchanger

- 8 in. SCHEDULE 100 (TYP)
- 2 in. SCHEDULE 60 RISERS
  ROTATED 45° & 135° RESPECTIVELY, ABOUT AXIS OF EXCHANGER.
- BAFFLES - 3/8" THICK, 1 FOOT APART AND EXTENDING OVER 1/2" OF THE INSIDE DIAMETER OF THE SHELL
- 1240 - 3/8 O.D. x .058 WALL TUBES ON 5/8 Δ PITCH
- 1 - 8 in. SCHEDULE 60 DOWNCOMER, ROTATED 90° ABOUT AXIS OF EXCHANGER
- MATL: ALL STAINLESS STEEL 347 EXCEPT DOWNCOMER AND RISERS WHICH ARE CARBON STEEL
- 2:1 ELLIPTICAL HEAD 2 7/16 THK. (TYP.)
if water is used for temporary shielding.

With a total fuel flow rate of 10,000 gpm, the exchangers transfer $2.74 \times 10^8$ Btu/hr (80,000 kw) total to produce 320,000 lb/hr of steam at 470°F and 515 psia with a steam quality of greater than 99%. The individual units design characteristics are given in Table VI, and the steam drum characteristics are given in Table VII.

**TABLE VI. Individual Heat Exchanger Design Data**

<table>
<thead>
<tr>
<th>Description</th>
<th>Specification</th>
</tr>
</thead>
<tbody>
<tr>
<td>Heat transferred</td>
<td>$6.85 \times 10^7$ Btu/hr (20,000 kw)</td>
</tr>
<tr>
<td>Steam flow</td>
<td>80,000 lb/hr (22.2 lb/sec)</td>
</tr>
<tr>
<td>Steam temperature</td>
<td>470°F</td>
</tr>
<tr>
<td>Steam pressure</td>
<td>515 psia</td>
</tr>
<tr>
<td>Design pressure (tubside)</td>
<td>2000 psia</td>
</tr>
<tr>
<td>Design pressure (shell side)</td>
<td>1500 psia</td>
</tr>
<tr>
<td>Design temperature</td>
<td>600°F</td>
</tr>
<tr>
<td>Steam quality</td>
<td>99%</td>
</tr>
<tr>
<td>Over-all heat transfer coefficient, fouled condition</td>
<td>705 Btu/hr-ft$^2$-°F</td>
</tr>
<tr>
<td>Feedwater temperature</td>
<td>374°F</td>
</tr>
<tr>
<td>Recirculation ratio</td>
<td>4:1</td>
</tr>
<tr>
<td>Heat transfer area</td>
<td>1885 ft$^2$</td>
</tr>
<tr>
<td>Number of tubes</td>
<td>1240</td>
</tr>
<tr>
<td>Effective length of tubes</td>
<td>15 1/2 ft</td>
</tr>
<tr>
<td>O D of tubes</td>
<td>0.375 in.</td>
</tr>
<tr>
<td>I D of tubes</td>
<td>0.259 in.</td>
</tr>
<tr>
<td>Tube thickness</td>
<td>0.058 in.</td>
</tr>
<tr>
<td>Tube material</td>
<td>SA 213 Type-347</td>
</tr>
</tbody>
</table>
**TABLE VI cont.**

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Velocity in tubes</td>
<td>12 ft/sec</td>
</tr>
<tr>
<td>Pressure drop</td>
<td>26.5 ft of fluid</td>
</tr>
<tr>
<td>O D shell</td>
<td>28-7/8 in.</td>
</tr>
<tr>
<td>I D shell</td>
<td>25-1/4 in.</td>
</tr>
<tr>
<td>Shell thickness</td>
<td>1-25/32 in.</td>
</tr>
<tr>
<td>Shell material</td>
<td>SA 240 Type-347</td>
</tr>
<tr>
<td>Tube sheet thickness</td>
<td>4-3/4 in.</td>
</tr>
<tr>
<td>Tube sheet material</td>
<td>SA 182 Type-347</td>
</tr>
<tr>
<td>Elliptical heat thickness</td>
<td>2-7/16 in.</td>
</tr>
<tr>
<td>Head material</td>
<td>SA 182 Type-347</td>
</tr>
</tbody>
</table>

**TABLE VII. Steam Drum Design Data**

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>O D boiler drum</td>
<td>52 in.</td>
</tr>
<tr>
<td>I D boiler drum</td>
<td>46-5/8 in.</td>
</tr>
<tr>
<td>Drum thickness</td>
<td>2-11/16 in.</td>
</tr>
<tr>
<td>Overall length *</td>
<td>20 ft</td>
</tr>
<tr>
<td>Material (drum and attachments)</td>
<td>SA 212-B</td>
</tr>
<tr>
<td>Size of steam risers</td>
<td>8 in. Sch. 60 pipe</td>
</tr>
<tr>
<td>Number of risers</td>
<td>2</td>
</tr>
<tr>
<td>Size of downcomers</td>
<td>8 in. Sch. 60 pipe</td>
</tr>
<tr>
<td>Number of downcomers</td>
<td>1</td>
</tr>
<tr>
<td>Drum steam outlet</td>
<td>10 in. Sch. 60 pipe</td>
</tr>
<tr>
<td>Number of steam outlets</td>
<td>3</td>
</tr>
<tr>
<td>Relief valve setting</td>
<td>1400 psia</td>
</tr>
</tbody>
</table>

*The drum length shall be of the order indicated, and it shall contain the required number of steam separators and scrubbers to insure a steam quality of at least 99%.*
The heat exchangers, boiler drum, and attachments shall be constructed in accordance with the appropriate paragraphs of the ASME Boiler and Pressure Vessel Code, Section VIII.

7.4 Reactor Circulating Pumps

The fuel solution is circulated to the heat exchangers and returned to the core by two 5000 gpm canned rotor pumps connected in parallel. The pumps supply 71 ft of head at full load conditions. It is anticipated that the pumps will be similar to the Westinghouse motor pump Type 12 CE modified in such a manner as to permit pumping uranyl sulfate solutions. Fig. 32 shows a typical pump of this type.

Two pumps were arbitrarily chosen in the belief that greater reliability of operation would be obtained. By connecting the pumps in parallel and with a check valve on each pump discharge, it will be possible to operate the system at about 80-90% of rated power with one pump out of service. Under these conditions the velocity decreases to about 70% of the normal value. The head loss has been calculated to be 37 ft of fluid at these lower velocities. At this head, the Type 12 CE pump delivers about 7000 gpm.

Calculations were made to investigate the flow velocities in the event both pumps stop operating. Under these conditions the fluid velocity drops very rapidly to about 3 ft/sec in the piping and to 1.8 ft/sec in the heat exchanger tubes. This natural convection system is adequate to remove almost 20,000 kw of heat from the core; this is more than adequate for the approximately 5,000 kw of decay heat.

The pressure drop calculations were based on the following equation

\[ h = \frac{f}{D} \frac{V^2}{2g} (L + n). \]  (7.4.1)
Figure 32 A Circulating Fuel Pump
where

\[ h = \text{head loss in feet of fluid} \]
\[ V = \text{fluid velocity, ft/sec} \]
\[ f = \text{friction factor, dimensionless} \]
\[ D = \text{diameter of pipe, ft} \]
\[ g_c = \text{gravitational constant, } \frac{\text{lbm-ft}}{\text{lb-f-sec}^2} \]
\[ L_e = \text{equivalent length of piping and fittings, ft} \]
\[ n = \text{number of velocity heads lost on entrance and exit to reactor and heat exchangers, dimensionless.} \]

Physical properties of the solution were taken from the work of Tobias (34).

### 7.5 Valves

The valves in the fuel system will be of the bellows seal type such as shown in Fig. 33. The primary seal is provided by a bellows on the valve stem which is backed up by a packing gland to minimize leakage in the event the bellows fail. Valve stem movement is obtained from a bellows air motor. Flanges are used for placing the valves in the system. They are located in such a manner that they are accessible from above for removal. It is anticipated that all control valves will be in one inch pipe line sizes and smaller (including the dump valve).

### 7.6 Liquid Level Controllers

The pressurizer liquid level is controlled by a device of the type shown in Fig. 34. The sensing element is a submerged float suspended by helical springs. Changes in level vary the buoyancy of the float, thus causing a movement of the core in a differential transformer. The core movement in the coil generates an electric current which produces a signal for the letdown valve operator. The float movements are dampened by use of a magnetic damper. The controller is located off to the side of the pressurizer drum and is flanged for remote removal.
Figure 33 High Pressure Control Valve
Figure 34  Liquid Level Controller
7.7 Oxygen System

Oxygen is required in the reactor for two very important reasons, namely an overpressure both to prevent boiling of the solution, and to aid in the control of corrosion. Consequently an oxygen supply must be available for the reactor. The supply would depend largely on the facilities available near the reactor site. If "technical" grade oxygen is readily available at the site, it would be convenient to store sufficient oxygen for startup (approximately 1200 standard ft$^3$) in bottles, with a little reserve. If this grade is not readily available, a small oxygen plant or a larger storage capacity would be needed. For one reactor it appears that sufficient oxygen storage would be preferable to operating a small oxygen plant in conjunction with the reactor; however for several reactors at remote locations the reverse might be true.

Recharging the system with oxygen would be carried out by connecting the oxygen bottles to the pressurizer and allowing them to reach an equilibrium pressure. At this pressure, two diaphragm type compressors, connected in parallel, are used to raise the pressure of the system. The compressors chosen for this operation are manufactured by Pressure Products Industries and are rated at 0.8 cfm at 2500 psi.

7.8 Dump Tanks

7.8.1 Criticality. The dump tank capacity for this reactor is such that the entire fuel inventory may be safety stored at all times, however they are not of the "ever-safe" type. The storage capacity is 350 ft$^3$ which represents a 10% excess over the loop volume.

On the basis of the systems studied for this reactor (see Sec. 3.1), it was desirous to pick an economical and reasonably safe geometry to
contain the fuel solution. The approach taken in sizing the dump tanks was to limit the maximum concentration of fissionable material per foot of dump tank length. In this manner, and with adequate mixing, the tanks would be safe and independent of the amount of solvent present for all operating conditions such as the evaporation process prior to startup or the draining and filling of the tanks. The tanks are equipped with evaporators to produce pure water for the initial filling of the system during startup and to provide mixing by recirculating the solution stored in the tanks.

Criticality calculations have been performed by Visner and Noderer (35) for infinitely long cylinders reflected by light water. Results were reported for 20°C systems both with and without cadmium coating. Their results for the cadmium coated cylinders are reproduced here in Fig. 35 and for the uncoated cylinders in Fig. 36. Also plotted in the figures are the linear concentrations as a function of tank diameter for the volumetric fuel concentration of each of the three reactor systems studied.

It is noted from the curves that the light water system exhibits a minimum linear concentration. This means that the dump tank diameter for the light water system should be such that the maximum possible linear concentration should not fall in the region to the right and above the minimum on the curve. If the design point does fall in that region, there is a possibility of the tanks going critical when solvent is evaporated. Similarly, the dump tank diameter for heavy water systems should be such that criticality is not obtained by evaporation. Based on Figs. 35 and 36 a fairly conservative dump tank diameter is tabulated for each system studied, along with other pertinent information, in Table VIII.
Fig. 35 - Criticality of Circular Cylinders of Infinite Length, Cadmium Coated, H₂O Reflected, 20°C
Fig. 36 - Criticality of Circular Cylinders of Infinite Length, H₂O Reflected, 20°C
TABLE VIII. Dump Tank Diameter

<table>
<thead>
<tr>
<th>Moderator</th>
<th>$^2$U$^{235}$ Enrichment (%)</th>
<th>Max. possible concentration (g/l)</th>
<th>Tank diameter (in)</th>
<th>Total length required (ft)</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>D$_2$O</td>
<td>20</td>
<td>5.2</td>
<td>20</td>
<td>162</td>
<td>Uncoated</td>
</tr>
<tr>
<td>D$_2$O</td>
<td>93</td>
<td>3.7</td>
<td>20</td>
<td>162</td>
<td>Uncoated</td>
</tr>
<tr>
<td>H$_2$O</td>
<td>93</td>
<td>12.8</td>
<td>16</td>
<td>250</td>
<td>Uncoated</td>
</tr>
<tr>
<td>D$_2$O</td>
<td>20</td>
<td>5.2</td>
<td>28</td>
<td>82</td>
<td>Cd coated</td>
</tr>
<tr>
<td>D$_2$O</td>
<td>93</td>
<td>3.7</td>
<td>28</td>
<td>82</td>
<td>Cd coated</td>
</tr>
<tr>
<td>H$_2$O</td>
<td>93</td>
<td>12.8</td>
<td>19</td>
<td>164</td>
<td>Cd coated</td>
</tr>
</tbody>
</table>

For a comparison to the calculations of Visner and Noderer, some experimental data$^{(35)}$ are plotted in Figure 36. From the data for H$_2$O moderated and reflected cylinders, it is seen that a 16-in. diameter tank (coated) is safe. The curve representing D$_2$O experimental data in Fig. 36 was extrapolated from some Los Alamos data$^{(36)}$ for a uranyl oxyfluoride fuel in D$_2$O where the predominant uranium isotope was U$^{235}$. The critical radius was obtained by equating the buckling of an infinite cylinder to the buckling of the finite cylinder reported in the data. An extrapolation distance of 2.5 cm was assumed in converting the data. The only reflector used in the experiments was the 1/8 in. thick stainless steel container. While these extrapolated D$_2$O data are probably not too valid for this analysis, it appears that a 20-in. diameter tank (uncoated) is safe.

It is seen from Table VIII that if cadmium is wrapped around the D$_2$O tanks, a much larger tank size can be used. This will reflect a considerable savings in the number of tanks used and appears to be a worthwhile investment.
for the D₂O systems. However, it may not be a good investment for the H₂O system since the sizes are not too different for coated or uncoated tanks.

There is a possibility of interaction between adjacent dump tanks if they are too close to each other. It is believed that if the tanks are placed on 6-ft centers and wrapped with cadmium, there should be no interaction between adjacent tanks. If the dump tanks are shielded with water, it probably should be borated. It is recognized that considerably more thought should be given to the dump tank arrangement and sizes, but time was not available for an extended study of this system. Whatever the final configuration, it appears that a critical experiment would be in order to verify the calculations.

7.8.2 Explosive Mixtures in Dump Tanks. The primary purpose of the dump tanks is to provide a storage volume for the loop inventory. Normally the tanks will have a very small volume of fuel solution held up in them, but upon a dump the entire inventory is stored. There is the ever present danger of explosive mixtures of radiolytic gases forming in the dump tanks during an emergency dump because of the steam stripping the gases out of solution.

Two ways of eliminating this problem are (1) to keep the tanks at a sufficient pressure to prevent mass boiling of the solution, and (2) to hold up the solution in the high pressure system long enough to obtain sufficient recombination of the gases. Fig. 37 shows the time required for recombination for a 0.02 m Cu^{++} concentration in D₂O solution. It is seen that the rate of recombination is very fast - 67% in 10 sec if the average temperature is 275°C, 85% if at 290°C. Therefore, if the dump signal has a few seconds delay, the partial pressure of the radiolytic deuterium
Fig. 37 - Catalytic Recombination of D₂ and O₂ in D₂O vs Time
(0.02 m Cu⁺⁺, Zero Power Generation)
would be reduced rapidly, and there would be no danger of building up an explosive mixture in the dump tanks. If an explosion does occur, it is believed that no harm will be done as the low pressure system is designed for 700 psi and should be able to withstand the pressures involved.

7.8.3 Design Temperature and Pressure. The equilibrium pressure in the dump tanks after a dump was calculated by assuming an adiabatic expansion of the high pressure fuel inventory. Volume heat sources were neglected, and the initial decay heat was assumed to be removed by the dump tank reflux condensers. Initial conditions of the fuel solution were taken as 1750 psia and 275°C. The only volumes considered as being useful in a dump were the dump tanks are the primary loop. The equilibrium pressure was calculated as 630 psia. It is recommended that the design pressure be taken as 700 psia at a design temperature of 600°F.

On the basis of these design conditions, the dump tanks are to be designed according to the ASME Boiler and Pressure Vessel Code, Section VIII subject to the data given in Table IX.

<table>
<thead>
<tr>
<th>TABLE IX, Dump Tank Design Data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Design pressure</td>
</tr>
<tr>
<td>Design temperature</td>
</tr>
<tr>
<td>Tank diameter (Cd coated)</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Tank volume</td>
</tr>
<tr>
<td>Material</td>
</tr>
<tr>
<td>Corrosion allowance</td>
</tr>
<tr>
<td>Shell and head thickness</td>
</tr>
</tbody>
</table>
7.9 Condensate Storage Tank

The volume of the storage tank is sufficient to store the expansion volume due to the startup procedure plus 50% excess. It has been calculated that the pure water expansion volume is 50 ft$^3$ when heated from 20°C to 200°C. Therefore the condensate storage tank has a volume of 75 ft$^3$. The tank is to be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section VIII and subject to the design data given in Table X.

<table>
<thead>
<tr>
<th>TABLE X. Condensate Storage Tank Design Data</th>
</tr>
</thead>
<tbody>
<tr>
<td>Design pressure</td>
</tr>
<tr>
<td>Design temperature</td>
</tr>
<tr>
<td>Volume</td>
</tr>
<tr>
<td>Material</td>
</tr>
</tbody>
</table>

7.10 Recombiners

Two recombiners have been provided in the low pressure system for the purpose of recombining the radiolytic gases which come out of solution upon a dump. One of the recombiners is coupled directly to the dump tank on the upstream side of the reflux condenser. The other is connected to the vapor space of the reflux condenser.

Since there is no gas letdown during steady state operation at full power, the recombiners will normally see service only during a dump. It is believed that a large amount of water vapor will reduce the efficiency of the catalyst for recombining the radiolytic gases during a dump. For this reason a second recombiner is placed near the outlet of the condenser.
Both of the recombiners are designed for the same duty. A space velocity of $420,000 \text{ hr}^{-1}$ is assumed to give 100% recombination in the catalyst bed consisting of $1/8$ by $1/8$ in., cylindrical pellets of 0.37% platinized aluminia. However, a safety factor of 2.5 should be included in the catalyst volume.

### 7.11 Reflux Condenser

The pressure in the dump tanks after a dump is lowered by the use of a water cooled reflux condenser. A standard two pass, floating tubesheet type exchanger with the condensing vapors on the shell side will be adequate if designed in accordance with the design data given in Table XI.

With the condenser described here, it will take about 5 minutes to remove the total heat from the system for an instantaneous dump time. Since it will take a finite time for the loop contents to flow into the dump tanks, it is estimated that the pressure will be reduced to atmospheric in about 10 minutes. This time will, of course, depend upon the size and length of the dump line.

### 7.12 Pressurizer Design

The determination of the vapor volume in the pressurizer was based on the following assumptions:

1. The system is initially operating at full power at an average temperature of $275^\circ \text{C}$ when the load is suddenly dropped, and the system temperature goes to $290^\circ \text{C}$.
2. Neither heat nor liquid is removed from the system until the dump valve opens at 2800 psia.
3. An adiabatic compression of the vapor space with no condensation of water vapor is assumed, thus there is no mass transfer between the liquid and vapor phases.
<table>
<thead>
<tr>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Heat load</td>
<td>$2.45 \times 10^5$ Btu/min</td>
</tr>
<tr>
<td>Condensing temperature</td>
<td>$212^\circ F$</td>
</tr>
<tr>
<td>LMTD</td>
<td>$82^\circ F$</td>
</tr>
<tr>
<td>Cooling water, inlet</td>
<td>$90^\circ F$</td>
</tr>
<tr>
<td>Cooling water, outlet</td>
<td>$160^\circ F$</td>
</tr>
<tr>
<td>Cooling water flow rate</td>
<td>4000 lb/min</td>
</tr>
<tr>
<td>Over-all heat transfer coefficient</td>
<td>410 Btu/hr-ft$^2$-°F</td>
</tr>
<tr>
<td>Velocity through tubes</td>
<td>5 ft/sec</td>
</tr>
<tr>
<td>Tube size</td>
<td>1/2 in. OD x 18 BWG</td>
</tr>
<tr>
<td>Tube pitch</td>
<td>5/8 in. triangular</td>
</tr>
<tr>
<td>Average tube length</td>
<td>6.75 ft</td>
</tr>
<tr>
<td>Number of tubes per pass</td>
<td>250</td>
</tr>
<tr>
<td>Number of passes</td>
<td>2</td>
</tr>
<tr>
<td>Shell diameter</td>
<td>18 in. ID</td>
</tr>
<tr>
<td>Material</td>
<td>18-8 Type-316 SS</td>
</tr>
<tr>
<td>Design pressure, shell side</td>
<td>700 psia</td>
</tr>
<tr>
<td>Design temperature</td>
<td>$600^\circ F$</td>
</tr>
</tbody>
</table>
The calculated change in volume of the system due to the above postulated excursion is 10.7 ft\(^3\). This represents the expulsion of about 16% of the core which, in turn, acts like a water piston to compress the vapor in the pressurizer. The normal free volume in the pressurizer is calculated to be 36 ft\(^3\). This will be sufficient to prevent the pressure from rising above 2800 psia on the above excursion. The pressurizer is mounted horizontally. Based on the above calculated free volume, a cylindrical vessel 30 in. ID by 8 ft long will provide, in addition to the free volume, about 9 in. of liquid depth for level control.

The vessel is to be constructed in accordance with the ASME Boiler and Pressure Vessel Code, Section VIII. The design pressure is 2000 psia at 600\(^\circ\)F. The vessel is to be fabricated from SA-212 Gr. B plate with a 10% integrally clad surface on the process side. The total thickness is 2.125 in. with the clad being 18-8 Type-347 SS.

7.13 Positive Displacement Feed Pumps

The fuel feed, moderator feed, and the purge water pumps are of the positive displacement diaphragm type. These pumps utilize the deflection of a diaphragm as the pumping action. The diaphragm is actuated by a reciprocating drive unit using oil as the fluid.

The largest suitable pump of this type, which has been developed at this time, has a capacity of about 1.5 gpm at 2000 psia. One each of these pumps is required for fuel feed, purge water for the circulating pumps, water feed for startup and dilution, and for pumping condensate from the dump tank condensers to the condensate storage tank.

7.14 Piping System and Holdup Volumes

All piping used in the primary system is 18-8 Type-347 SS. In sizes
8 in. and larger, welded piping has been assumed while the smaller sizes are assumed to be seamless. The piping system is designed according to ASA B31.1 - 1955 with a weld joint efficiency of 95%. A corrosion allowance of 0.1 in. and a flow velocity of 22 ft/sec maximum have been assumed in sizing the piping. This velocity is safely below the critical velocity of 26 ft/sec at 250°C.

The fuel volume in the primary system is about 8,915 l with an estimated 1,085 l holdup in the dump tanks and small lines for a total inventory of about 10,000 l. The surface area exposed to fuel solution is about 1,025,000 in². Volumes, surface areas, and velocities for the primary system are tabulated in Table XII.

7.15 Containment Vessel

A cylindrical containment vessel was arbitrarily chosen to retain radioactive gases in the event of a catastrophic accident. It was assumed that 75% of the containment vessel volume would be available for expansion of the primary loop inventory. For a vessel diameter of 42 ft and a height of about 90 ft, the equilibrium pressure was estimated as 30 psia for an adiabatic expansion of the fluid. The lower portion of the vessel will be underground to reduce shielding and to facilitate removal and replacement of the equipment within the vessel.

The vessel will have a large air lock type entrance at ground level. The size of the door will be about 10 by 15 ft high. A small conventional size air lock passageway will permit entry at the ground level for routine maintenance at shutdown. A circular crane track at the top of the vessel will provide 20 ton crane service inside for handling shielding plugs and equipment.
<table>
<thead>
<tr>
<th><strong>TABLE XII. Volumes, Areas, and Velocities</strong></th>
<th>Volume ((l))</th>
<th>Area ((in^2))</th>
<th>Velocity ((ft/sec))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core</td>
<td>1,860</td>
<td>11,300</td>
<td>---</td>
</tr>
<tr>
<td>Annuli between thermal shields</td>
<td>700</td>
<td>84,800</td>
<td>---</td>
</tr>
<tr>
<td>Core outlet annulus</td>
<td>90</td>
<td>3,700</td>
<td>18.8</td>
</tr>
<tr>
<td>Core to pump (2)</td>
<td>1,180</td>
<td>26,700</td>
<td>19.0</td>
</tr>
<tr>
<td>12 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Pump</td>
<td>60</td>
<td>1,400</td>
<td>---</td>
</tr>
<tr>
<td>Pump to &quot;Y&quot; (2)</td>
<td>680</td>
<td>15,400</td>
<td>19.0</td>
</tr>
<tr>
<td>12 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Header</td>
<td>580</td>
<td>9,200</td>
<td>18.9</td>
</tr>
<tr>
<td>18 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Header to HX (4)</td>
<td>370</td>
<td>12,500</td>
<td>21.4</td>
</tr>
<tr>
<td>8 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Heat exchanger (4)</td>
<td>1,240</td>
<td>807,000</td>
<td>12.0</td>
</tr>
<tr>
<td>HX to header (4)</td>
<td>370</td>
<td>12,500</td>
<td>21.4</td>
</tr>
<tr>
<td>8 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Header to core</td>
<td>1,050</td>
<td>16,800</td>
<td>18.9</td>
</tr>
<tr>
<td>18 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Standpipe to pressurizer</td>
<td>450</td>
<td>11,500</td>
<td>---</td>
</tr>
<tr>
<td>10 in. Sch. 120 pipe</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Pressurizer</td>
<td>285</td>
<td>3,600</td>
<td>---</td>
</tr>
<tr>
<td>Sub total</td>
<td>8,915</td>
<td>1,016,000</td>
<td></td>
</tr>
<tr>
<td>Dump and small lines</td>
<td>1,085</td>
<td>9,000</td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>10,000</td>
<td>1,025,000</td>
<td></td>
</tr>
</tbody>
</table>
The vessel shall be designed and fabricated according to accepted engineering practices and techniques such that a vessel of the highest integrity is obtained. It shall be designed to withstand 30 psi internal pressure at a temperature of 150°F. The portion above ground shall be designed to withstand wind velocities of 2 to 180 mph, and the entire structure designed for relatively severe earthquakes.

7.16 Shielding

The philosophy of the shield design was mentioned in Section 7.1. The shielding (Fig. 27) is provided by concrete walls and slabs as well as by the earth external to the containment vessel.

The shield design was based on a maximum radiation level above the concrete slab at ground elevation of the order of 4000 Mev/cm²·sec or 30 fast neutrons/cm·sec or a combination of both to give an equivalent dose. The calculated dose, as shown in Table XIII, exceeded the above values somewhat, however an order of magnitude calculation was considered reasonable since the containment vessel will not be occupied when the reactor is in operation. Since the containment vessel excludes personnel from the region of high dose rates, the geometric attenuation will reduce the radiation external to the vessel to a safe level for personnel.

The thickness of shielding was determined by the relaxation length method. It was assumed that the reactor was operating at full power and that the fission products present were the results of one year of continuous operation. The containment vessel walls around the equipment area (below the grade) will be 2 ft of normal density concrete. At least 6 ft of earth is required external to these walls to reduce the radiation level to an inhabitable value. The slab above the reactor core will be 3 ft of
high density concrete and the slab at ground level is 3.75 ft of the same kind of concrete.

Table XIII indicates approximate radiation levels inside the containment vessel.
TABLE XII. Radiation Levels in Containment Vessel

<table>
<thead>
<tr>
<th></th>
<th>Fast Neutrons (neuts/cm²-sec)</th>
<th>Primary Gamma (Mev/cm²-sec)</th>
<th>Secondary Gamma (Mev/cm²-sec)</th>
<th>F. P. from Auxilaries, etc. (Mev/cm²-sec)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outside reactor pressure vessel</td>
<td>$2.9 \times 10^{12}$</td>
<td>$11.7 \times 10^{13}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Below first horizontal slab</td>
<td>$3.5 \times 10^{11}$</td>
<td>$1.4 \times 10^{13}$</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Above first horizontal slab</td>
<td>$1.75 \times 10^{5}$</td>
<td>$1.7 \times 10^{7}$</td>
<td>$3.18 \times 10^{8}$</td>
<td></td>
</tr>
<tr>
<td>Below second horizontal slab</td>
<td>$1.5 \times 10^{4}$</td>
<td>$1.46 \times 10^{6}$</td>
<td>$2.74 \times 10^{7}$</td>
<td>$28.5 \times 10^{9}$</td>
</tr>
<tr>
<td>Above second horizontal slab</td>
<td>$\sim 0$</td>
<td>0.5</td>
<td>8.4</td>
<td>5520*</td>
</tr>
</tbody>
</table>

*Broken up as follows

<table>
<thead>
<tr>
<th></th>
<th>Pumps (Mev/cm²-sec)</th>
<th>Pressurizer (Mev/cm²-sec)</th>
<th>Heat Exchanger (Mev/cm²-sec)</th>
<th>Total Mev/cm²-sec</th>
</tr>
</thead>
<tbody>
<tr>
<td>Below second horizontal slab</td>
<td>$6.9 \times 10^{8}$</td>
<td>$15.9 \times 10^{9}$</td>
<td>$12.8 \times 10^{9}$</td>
<td>$2.94 \times 10^{10}$</td>
</tr>
<tr>
<td>Above second horizontal</td>
<td>210</td>
<td>1380</td>
<td>3930</td>
<td>5520</td>
</tr>
</tbody>
</table>

Note: The radiation level at the pump position is of the order of $3 \times 10^{10}$ Mev/cm²-sec.
CHAPTER 8 COST ANALYSIS

8.1 Cost Analysis

It was previously shown that the H₂O-20% enriched fuel system can be eliminated on the basis of corrosion. Therefore, this analysis will be concerned with the three remaining systems (H₂O fully enriched uranium, D₂O-20% enriched uranium, and D₂O-fully enriched uranium).

In making the cost comparison of the three systems, it is assumed that all costs are the same for the three systems except for the fuel burnup and inventory costs.

Estimates for the costs of the primary plant (Tables XV and XVI) were obtained from data in TBR Memorandum No. 4 (38) and information obtained from Alan Colyer (39) of the Foster-Wheeler Corp. Estimates of the turbo-generator plant cost (Table XVI) were obtained from R. C. Robertson of ORNL (40).

Amortization of the reactor is based on an assumed value of 5% return on capital investment and a 25-year operating life.

The cost of fuel (41) (90% enriched U²³⁵ at $17.07/g of contained U²³⁵ and 20% enriched at $16.12/g of contained U²³⁵), the cost of (19) D₂O($28.00/lb), and the inventory charges (4%) are current AEC prices.

It can be seen from Table XVIII that the numbers are not sufficiently accurate to choose between the three systems on an economic basis. However, because of the availability of H₂O, and because the maintenance problems will be considerably eased by the use of H₂O (leakage of H₂O from the system into the flooded cell will not matter), the H₂O system was chosen. Also, it should be noted that the waste from chemical processing can be thrown away immediately, because there is no need for H₂O recovery.
8.2 Summary of Costs

The summary of costs is recorded in Table XIV. The total cost of power based on these estimates is about 18.8 mills/net kwhr. It should be pointed out that these costs are not based on a thorough estimating procedure. No special costs are included for the unique requirements in design, construction, and operation for remote locations.

### Table XIV. Estimated Capital Investment and Energy Costs

<table>
<thead>
<tr>
<th>A. Capital Investment</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Primary Plant (from Table XV)</td>
<td>$10,900,000</td>
</tr>
<tr>
<td>2. Turbo-generator plant</td>
<td>3,100,000</td>
</tr>
<tr>
<td>3. Reactor start-up and testing</td>
<td>500,000 $14,500,000</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>B. Fixed Annual Costs</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Fixed charges on capital investment at 12.7%</td>
<td>$1,842,000</td>
</tr>
<tr>
<td>2. Fixed charges on special material inventory (fuel at 4%)</td>
<td>84,000</td>
</tr>
<tr>
<td>3. Cost of fuel burnout</td>
<td>467,000</td>
</tr>
<tr>
<td>4. Operation and maintenance</td>
<td>250,000 $2,643,000</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>C. Total Energy Costs, mills/net kwhr</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Fuel consumption</td>
<td>3.3</td>
</tr>
<tr>
<td>2. Fixed annual cost at 80% plant capacity factor</td>
<td></td>
</tr>
<tr>
<td>Capital investment</td>
<td>13.1</td>
</tr>
<tr>
<td>Special inventory</td>
<td>.6</td>
</tr>
<tr>
<td>Operation and maintenance</td>
<td>1.8 15.5 18.8 mills/net kwhr</td>
</tr>
</tbody>
</table>
### TABLE XV. Estimated Installed Cost of Primary Plant

<table>
<thead>
<tr>
<th>Item</th>
<th>Cost</th>
</tr>
</thead>
<tbody>
<tr>
<td>High pressure system</td>
<td>$3,393,000</td>
</tr>
<tr>
<td>Building</td>
<td>900,000</td>
</tr>
<tr>
<td>Off-gas system</td>
<td>100,000</td>
</tr>
<tr>
<td>Storage pool</td>
<td>150,000</td>
</tr>
<tr>
<td>Chemical processing equipment</td>
<td>750,000</td>
</tr>
<tr>
<td>Instrumentation and special handling equipment</td>
<td>450,000</td>
</tr>
<tr>
<td><strong>Total direct cost</strong></td>
<td><strong>$5,743,000</strong></td>
</tr>
</tbody>
</table>

- General contractor overhead and profit, 25% $1,386,000
- Erection, 15% $831,000
- Architect-engineer fee, including supervision of construction, 15% $831,000
- Contingencies, 20% $1,109,000
- Special systems and component engineering and development $1,000,000

**Total (brought forward to Table XIV)** $10,900,000
TABLE XVI. Breakdown of Estimated Direct Cost of Primary Plant

A. High Pressure System

<table>
<thead>
<tr>
<th>Item</th>
<th>Cost</th>
</tr>
</thead>
<tbody>
<tr>
<td>Reactor vessel including thermal neutron shield</td>
<td>$400,000</td>
</tr>
<tr>
<td>Heat exchangers (4)</td>
<td>750,000</td>
</tr>
<tr>
<td>Circulating pumps (2)</td>
<td>200,000</td>
</tr>
<tr>
<td>Feed pumps</td>
<td>60,000</td>
</tr>
<tr>
<td>Pressurizer</td>
<td>65,000</td>
</tr>
<tr>
<td>Steam drum</td>
<td>50,000</td>
</tr>
<tr>
<td>Dump tanks</td>
<td>150,000</td>
</tr>
<tr>
<td>Piping, elbows, fittings, etc.</td>
<td>720,000</td>
</tr>
<tr>
<td>Insulation of piping</td>
<td>400,000</td>
</tr>
<tr>
<td>Concrete lining</td>
<td>150,000</td>
</tr>
<tr>
<td>Containment vessel</td>
<td>150,000</td>
</tr>
<tr>
<td>Miscellaneous</td>
<td>98,000</td>
</tr>
<tr>
<td>Waste system</td>
<td>200,000</td>
</tr>
</tbody>
</table>

$43,393,000

B. Steam System

<table>
<thead>
<tr>
<th>Item</th>
<th>Cost</th>
</tr>
</thead>
<tbody>
<tr>
<td>Turbo-generator</td>
<td>$1,300,000</td>
</tr>
<tr>
<td>Foundations</td>
<td>375,000</td>
</tr>
<tr>
<td>Condenser</td>
<td>300,000</td>
</tr>
<tr>
<td>Feed-water pumps</td>
<td>200,000</td>
</tr>
<tr>
<td>Piping</td>
<td>300,000</td>
</tr>
<tr>
<td>Electrical accessories</td>
<td>250,000</td>
</tr>
<tr>
<td>Outdoor switchyard</td>
<td>375,000</td>
</tr>
</tbody>
</table>

$3,100,000
<table>
<thead>
<tr>
<th>Item</th>
<th>Cost (in dollars)</th>
</tr>
</thead>
<tbody>
<tr>
<td>C. Building</td>
<td>900,000</td>
</tr>
<tr>
<td>D. Off-gas system</td>
<td>100,000</td>
</tr>
<tr>
<td>E. Chemical processing equipment (hot cells, hydroclones, etc.)</td>
<td>750,000</td>
</tr>
</tbody>
</table>

**TABLE XVII. Fixed Charges Based on a 25-Year Capital Recovery Period**

<table>
<thead>
<tr>
<th>Description</th>
<th>Percentage</th>
</tr>
</thead>
<tbody>
<tr>
<td>Return and sinking fund amortization at 5%</td>
<td>7.10%</td>
</tr>
<tr>
<td>Federal income tax</td>
<td>3.25%</td>
</tr>
<tr>
<td>State income tax</td>
<td>0.26%</td>
</tr>
<tr>
<td>Ad valorem tax</td>
<td>2.00%</td>
</tr>
<tr>
<td>Insurance and miscellaneous</td>
<td>0.10%</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>12.71%</strong></td>
</tr>
</tbody>
</table>
TABLE XVIII. Fuel and Moderator Costs

<table>
<thead>
<tr>
<th>Item</th>
<th>D₂O - 20%</th>
<th>D₂O - 93%</th>
<th>H₂O - 93%</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cost of (^{235})U (^{235}) (^{235}) Inventory at 4% rental/yr</td>
<td>$47,100</td>
<td>$38,900</td>
<td>$81,900</td>
</tr>
<tr>
<td>Cost of D₂O (^{235}) Inventory at 4% rental/yr</td>
<td>$26,700</td>
<td>$26,700</td>
<td>---</td>
</tr>
<tr>
<td>Cost of D₂O (^{235}) (^{235}) Leakage at 2%/yr</td>
<td>$13,350</td>
<td>$13,350</td>
<td>---</td>
</tr>
<tr>
<td>Burnup at 80% Load Factor</td>
<td>$441,000</td>
<td>$467,000</td>
<td>$467,000</td>
</tr>
<tr>
<td>30-day (^{235})U (^{235}) (^{235}) Inventory at 4% rental/yr</td>
<td>$1,840</td>
<td>$1,950</td>
<td>$1,950</td>
</tr>
<tr>
<td>30-day D₂O (^{235}) (^{235}) Inventory at 4% rental/yr</td>
<td>$1,110</td>
<td>$1,110</td>
<td>---</td>
</tr>
<tr>
<td><strong>TOTALS</strong></td>
<td><strong>$551,100</strong></td>
<td><strong>$549,010</strong></td>
<td><strong>$550,850</strong></td>
</tr>
</tbody>
</table>
APPENDIX A  DATA AND EQUATIONS FOR SAFETY AND STABILITY CALCULATIONS

A.1 Maximum Pressure Rise

Kasten (22) developed an approximate equation to describe the maximum pressure rise expected in an aqueous homogeneous reactor as

\[
P_{\text{max}} = \frac{m_e \bar{F}}{2 \omega_n^2 \gamma_2} \left[ 0.385 \frac{m_e}{4 \gamma_3} \right] + \frac{C_2 m_e}{\gamma_2}, \tag{A.1.1}
\]

where

\[
\bar{F} = 1 + \frac{1}{2} \left[ C_2 + \frac{(\gamma_f + m_e)\omega_0^2}{\omega_n^2} \right]. \tag{A.1.2}
\]

The symbols are defined in Table XIX.

A.2 Equivalent Prompt Reactivity and Linear Rate Addition Relationship

As was mentioned before, the buildup of power is not particularly rapid as reactivity increases until the reactor reaches prompt critical. The reactor power at prompt critical can be determined by the use of Fig. 38 (taken from Ref. 22) where the quantity \(x_{pc}\) is the relative increase in reactor power.

The relationship between equivalent prompt reactivity and linear rate reactivity is determined from Fig. 39 (from Ref. 22). The square of the modified nuclear frequency, necessary for the use of Fig. 39 is obtained from the following equation:

\[
\omega_{np}^2 = \omega_n^2 x_{pc} = \frac{1}{\ell} \left| \frac{\partial k_0}{\partial T} \right| \frac{P_0}{S_c} x_{pc}. \tag{A.2.1}
\]

A.3 Nomenclature and Data

The symbols used in this appendix and in Chapter 4 are defined in Table XIX.
Figure 38  Neutron Power at Prompt Critical Relative to Initial Power

\[ x_{pc} = \exp \left( -\frac{M_s^2}{2 \xi} \right) + \sqrt{\pi} \sqrt{\frac{M_s}{\sqrt{2 \xi}}} \text{ erf} \left( \frac{M_s}{\sqrt{2 \xi}} \right) \]
Figure 39  Relation Between Equivalent Prompt Reactivity and Rate of Reactivity Addition

\[ \Delta k_{\text{eq}} = \text{EQUIVALENT PROMPT REACTIVITY} \]

\[ \lambda = \text{MEAN LIFETIME OF PROMPT NEUTRONS} \]

\[ b = \text{RATE OF REACTIVITY ADDITION} \]

\[ \omega_{np}^2 = \text{MODIFIED NUCLEAR FREQUENCY} \]
### TABLE XIX. Nomenclature and Data for Safety and Stability Calculations

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>cross sectional area of core relief pipe, 0.474 ft² (nominal 10 in. pipe)</td>
</tr>
<tr>
<td>b</td>
<td>linear rate of reactivity addition, Δkₑ/sec</td>
</tr>
<tr>
<td>c_p</td>
<td>heat capacity of fluid, 2.35 kw sec/lb°C at 280°C and 1750 psia</td>
</tr>
<tr>
<td>c_2</td>
<td>= \frac{144g_o P_o nV_c}{V_s^2/\rho_o V_p} = a measure of the effect of pressurizer volume upon core pressure rise, dimensionless</td>
</tr>
<tr>
<td>g_c</td>
<td>dimensional constant, 32.2 ft lb mass/sec² lb force</td>
</tr>
<tr>
<td>k_e</td>
<td>effective multiplication constant, dimensionless</td>
</tr>
<tr>
<td>kₑ(0)</td>
<td>initial value of kₑ</td>
</tr>
<tr>
<td>kₑ⁻¹</td>
<td>reactivity, dimensionless</td>
</tr>
<tr>
<td>Δkₑ⁻¹</td>
<td>reactivity above prompt critical, refers to the amount of reactivity which, if added instantaneously, would result in the same pressure rise as that obtained if the reactivity were added at a specific rate, dimensionless</td>
</tr>
<tr>
<td>\frac{dkₑ}{d(\ell/C)}</td>
<td>concentration coefficient of reactivity, dimensionless, 0.46</td>
</tr>
<tr>
<td>\frac{dkₑ}{dT}</td>
<td>temperature coefficient of reactivity, (-5.1 \times 10^{-4} \frac{Δkₑ}{kₑ}^{⁰°C}) at 280°C</td>
</tr>
<tr>
<td>\frac{dkₑ}{d\rho}</td>
<td>density coefficient of reactivity, (3.6 \times 10^{-3} \frac{Δkₑ}{kₑ}^{⁰°C}) at (46.9 \text{ lb/ft}^3) (280°C)</td>
</tr>
<tr>
<td>\ell</td>
<td>average lifetime of prompt neutrons, (1.45 \times 10^{-4}) sec for &quot;clean&quot; condition at 280°C</td>
</tr>
<tr>
<td>L</td>
<td>length of pipe between reactor core and pressurizer, 30 ft</td>
</tr>
<tr>
<td>\μₑ</td>
<td>equivalent prompt reactivity addition divided by mean lifetime of prompt neutrons, sec⁻¹</td>
</tr>
<tr>
<td>n</td>
<td>ratio of heat capacity at constant pressure to heat capacity at constant volume for pressurizing fluid, dimensionless, 1.32</td>
</tr>
</tbody>
</table>

\(\ell\) = average lifetime of prompt neutrons, \(1.45 \times 10^{-4}\) sec for "clean" condition at 280°C.
\( n_f = \text{number of velocity heads of fluid lost while fluid moves from core to pressurizer, dimensionless, 5.5 for safety, 1.8 for stability} \)

\( p = \text{rise in core pressure, psi} \)

\( p_{\text{max}} = \text{maximum value of } p \text{ following a reactivity addition, psi} \)

\( P = \text{reactor power, kw} \)

\( P_0 = \text{reactor power at initial conditions, kw} \)

\( P_{\text{pc}} = \text{reactor power at prompt critical, kw} \)

\( S_c = \text{volume heat capacity of core, } 7234 \text{ kw sec}^2/\text{C} \)

\( t = \text{time, sec} \)

\( U_o = \text{average velocity of fluid in core exit pipe evaluated at initial conditions, 22.8 ft/sec for safety (based on relief pipe area), 16.6 ft/sec for stability} \)

\( v_S = \text{acoustic velocity in core fluid, ft/sec} \)

\[
\frac{1}{\sqrt{\frac{\rho}{\mu}}} \frac{144 \ g_c}{s^2} v_s^2 = 7.3 \times 10^6 \text{ ft}^2/\text{sec}^2 \text{ at } 280^\circ \text{C}
\]

\( V_c = \text{volume of core region, 65.5 ft}^3 \)

\( V_p = \text{volume of pressurizing fluid, 36.0 ft}^3 \)

\( x = \frac{P}{P_0} = \text{relative reactor power, dimensionless} \)

\( x_{\text{pc}} = \frac{P_{\text{pc}}}{P_0} = \text{relative reactor power at prompt critical, dimensionless} \)

\( \beta = \text{effective fraction of fission neutrons which are delayed, dimensionless, 0.318} \)

\( \gamma_f = \frac{n_f U_o}{L} = \text{normalized friction coefficient, sec}^{-1} \)

\( \gamma_2 = \frac{144 \ g_c}{V_s^2 \ell} \frac{dk_e}{d\rho} = \text{conversion factor, (psi-sec)}^{-1} \)

\( \gamma_3 = \frac{A U_o}{V_c \ell} \frac{dk_e}{\rho} = \text{conversion factor, sec}^{-2} \)
\[ \rho = \text{average density of fuel fluid, lb/ft}^3 \]

\[ \rho_0 = \text{average density of fuel at initial condition, } 46.9 \text{ lb/ft}^3 \text{ at 1750 psia and } 280°C \]

\[ \omega_h^2 \approx \frac{A \gamma}{V_c L} = \text{square of hydraulic frequency, sec}^{-2} \]

\[ \omega_n^2 = \frac{1}{k} \left| \frac{\partial k_e}{\partial T} \right| \frac{F_0}{S_c} = \text{square of nuclear frequency, sec}^{-2} \]

\[ \omega_{np}^2 = \omega_n^2 \frac{x_{pc}}{S_c} = \text{square of nuclear frequency at prompt critical, sec}^{-2} \]
APPENDIX B  DETERMINATION OF PRESSURE VESSEL THICKNESS

B.1 Introduction

Nuclear reactor pressure vessels are subjected to radiation heating of sufficient magnitude to cause severe thermal stresses. These stresses must be added to the pressure stresses, and their sum must not exceed a safe operating stress. Both the pressure and thermal stress will have their maximum value at the inside surface of the vessel wall. Therefore, the proper vessel thickness can be determined from a graph of the maximum total stress as a function of shell thickness.

In order to calculate the thermal stress, the radiation heating in the vessel wall must be calculated by some acceptable method. From the heat generation expression, the temperature distribution can be determined; and from the temperature distribution, the thermal stress can be determined.

The pressure stress is calculated from the usual stress equation. For a homogeneous vessel material, the pressure stress is due to the hydraulic pressure of the vessel contents. However, if a clad vessel is used, an additional stress caused by differential expansion may be imposed upon the base material. This additional loading acts similar to the hydraulic pressure and may be simply treated as an increase in the design pressure.

B.2 Radiation Sources

The heat generated within the vessel wall arises from the attenuation of the radiation escaping from the reactor core. Fast neutron reactions were not considered in the calculations since they were negligible in comparison to the thermal neutron reactions for the thicknesses of interest. The sources of radiation considered were:
1) Prompt fission gammas - Five photons per fission with an average energy of 1 Mev each.

2) Delayed fission gammas - Six photons per fission with an average energy of 1 Mev each.

3) Core capture gammas - One photon per nonfission capture in uranium with an average energy of 6 Mev each. (Captures in other constituents were neglected).

4) Secondary capture gammas - One photon per capture in the iron pressure vessel with an average energy of 6 Mev each.

B.3 Radiation Leakage from Core

The nomenclature used throughout this appendix is given in Table XX.

In order to calculate the radiation attenuation within the vessel wall, it is necessary to know the amount of radiation leaving the core. The net neutron current, \( J_o \), entering the vessel (or thermal shield) was calculated from the diffusion theory approximation

\[
J_o = -D \left. \frac{d\Phi}{dr} \right|_R .
\]  

(B.3.1)

Since three reactors were under consideration, the one with the largest leakage was chosen for these calculations. This reactor (i.e. \( D_2O \) - fully enriched uranyl sulfate) has an estimated leakage at the inside surface of the thermal shield of \( 3.97 \times 10^{14} \) neuts/cm\(^2\)-sec.

The gamma flux at the surface was calculated for each of the core gammas from the equation (42) for a uniform spherical source with self-absorption:

\[
I_o = \frac{R S_Y}{2\mu_s} \left[ 1 - \frac{1}{2\mu_s R} + \frac{e^{-2\mu_s R}}{2\mu_s R} \right] .
\]  

(B.3.2)
The volumetric source term, $S_v$, was taken as

$$S_v = n\Phi$$

(B.3.3)

where the buildup factor, $B$, was taken as unity. The results of this equation were compared to the results of a numerical integration of an equation reported by Chapman(43) in which it is possible to account for radial distribution and buildup by use of the NDA buildup factors. The best comparison of Eq. (B.3.2) (made for prompt fission gammas) was for a buildup factor of unity. Consequently the above expression was used to evaluate the gamma flux at the surface of the thermal shield. Based on the sources given in paragraph B.2 and the data in Tables XX and XXI, the 1 Mev gamma flux entering the thermal shield was taken as $I_{0,1} = 15.4 \times 10^{13}$ $\gamma$/cm$^2$-sec (combined prompt fission and decay gammas) with no angular distribution being prescribed. The 6 Mev gamma flux was taken as $I_{0,6} = 3.5 \times 10^{12}$ $\gamma$/cm$^2$-sec with no angular distribution being prescribed.

B.4 Radiation Heating

The heating within the thermal shield and pressure vessel was calculated on the basis of slab geometry. For the secondary capture gamma heating, the following equation(43) was used

$$G(x) = n\gamma\nu_{e} \frac{J_0}{2} F(\beta, \mu x).$$

(B.4.1)

If a buildup factor, $B$, is taken as

$$B = A e^{-\alpha_1 \mu x} + (1-A) e^{-\alpha_2 \mu x}$$

(B.4.2)

where the parameters are evaluated from the NDA buildup factors(44), then it follows that Eq. (B.4.1) becomes

$$G(x) = \frac{n\gamma\nu_{e} J_0}{2} \left[ A F(\beta_1, \mu x) + (1-A) F(B_2 \mu x) \right]$$

(B.4.3)
where

\[ \sigma_{p,\text{max}} = \frac{P}{b^3-a^3} \left[ \frac{2a^3+b^3}{2a^3} \right] \]  \hspace{1cm} (B.6.1)

The work of Hughes\(^{(45)}\) was used to evaluate an equivalent pressure due to the stainless steel trying to expand more than the carbon steel. It was found that this additional pressure amounted to about 550 psi. Consequently the design pressure was increased to 2550 psia to account for the differential expansion.

The thermal stresses also have their maximum value at the inside surface. Since the ratio of vessel thickness to radius is small, the equation for a slab was used as

\[ \sigma_{t,\text{max}} = \frac{aE\theta}{1-\nu} \]  \hspace{1cm} (B.6.2)

Several vessel thicknesses were assumed and the stresses calculated with thermal shield thickness as a parameter. The results where the pressure stress curves have been omitted for clarity are shown in Fig. 41. It has been assumed in the calculations that the thermal shields are 1-in. thick with a 1-in. thick coolant flow annulus between the shields. No credit was taken for the attenuation of the radiation by the coolant between shields. The physical properties of the steel were taken for a design temperature of 600°F.

The base material has been taken as SA 302 with an additional 10% clad of 18-8 Type-347 SS. The base material has an allowable stress of 20,000 psi at 600°F, therefore the vessel thickness is determined from Fig. 41 as 3 in. with 3 in. of thermal shield. The clad thickness will be 0.3 in.

**B.7 Nomenclature and Data**

The nomenclature and data used in the expressions for determining the vessel thickness are tabulated in Tables XX and XXI.
Fig. 41 - Maximum Stresses in Vessel Wall (both surfaces at same temperature)
TABLE XX. Nomenclature and Data for Stress Calculations

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>coefficient, dimensionless</td>
<td>2.9</td>
</tr>
<tr>
<td>a</td>
<td>inside radius of pressure vessel, cm</td>
<td></td>
</tr>
<tr>
<td>B</td>
<td>buildup factor, dimensionless</td>
<td></td>
</tr>
<tr>
<td>b</td>
<td>outside radius of pressure vessel, cm</td>
<td></td>
</tr>
<tr>
<td>D</td>
<td>thermal neutron diffusion length, 1.23 cm</td>
<td></td>
</tr>
<tr>
<td>E</td>
<td>modulus of elasticity, $26.9 \times 10^6$ psi</td>
<td></td>
</tr>
<tr>
<td>$E_\gamma$</td>
<td>gamma energy, Mev</td>
<td></td>
</tr>
<tr>
<td>$F(\beta, \mu x)$</td>
<td>absorption function, dimensionless, (See reference 44)</td>
<td></td>
</tr>
<tr>
<td>G(x)</td>
<td>volumetric heat generation function, Mev/cm$^3$-sec</td>
<td></td>
</tr>
<tr>
<td>$G_o$</td>
<td>volumetric heat generation at x=0, Mev/cm$^3$-sec</td>
<td></td>
</tr>
<tr>
<td>$I_o$</td>
<td>gamma flux entering thermal shield, photons/cm$^2$-sec</td>
<td></td>
</tr>
<tr>
<td>$J_o$</td>
<td>net neutron current entering thermal shield, neutrons/cm$^2$-sec</td>
<td></td>
</tr>
<tr>
<td>k</td>
<td>thermal conductivity, $1.75 \times 10^{12}$ Mev/cm$\cdot$sec$^{-0\circ F}$</td>
<td>(29.2 Btu/hr-ft$^{-0\circ F}$)</td>
</tr>
<tr>
<td>m</td>
<td>heat generation attenuation coefficient, cm$^{-1}$</td>
<td></td>
</tr>
<tr>
<td>n</td>
<td>number of photons emitted per neutron reaction, dimensionless</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>pressure including differential expansion, psia</td>
<td></td>
</tr>
<tr>
<td>$q^m$</td>
<td>total volumetric heat source, Mev/cm$^3$-sec</td>
<td></td>
</tr>
<tr>
<td>R</td>
<td>radius of core, 76.2 cm</td>
<td></td>
</tr>
<tr>
<td>$S_v$</td>
<td>volumetric gamma source, photons/cm$^3$-sec</td>
<td></td>
</tr>
<tr>
<td>t</td>
<td>vessel thickness, cm</td>
<td></td>
</tr>
<tr>
<td>x</td>
<td>distance from inside surface of vessel, cm</td>
<td></td>
</tr>
<tr>
<td>$a$</td>
<td>coefficient of expansion, $7.6 \times 10^{-6}$ in./in.$^{-0\circ F}$</td>
<td></td>
</tr>
<tr>
<td>$a_1$</td>
<td>coefficient, dimensionless, $-8.6 \times 10^{-2}$</td>
<td></td>
</tr>
<tr>
<td>$a_2$</td>
<td>coefficient, dimensionless, $10.1 \times 10^{-2}$</td>
<td></td>
</tr>
<tr>
<td>$\beta$</td>
<td>$\kappa/\mu$, dimensionless</td>
<td></td>
</tr>
</tbody>
</table>
\[ \beta_1 = \frac{1}{1 + \alpha_1} \mu, \text{ dimensionless} \]
\[ \beta_2 = \frac{1}{1 + \alpha_2} \mu, \text{ dimensionless} \]
\[ \Theta(x) = \text{temperature at point } x \text{ (above surface temperature), } ^\circ F \]
\[ \bar{\Theta} = \text{average temperature difference, } ^\circ F \]
\[ \lambda = \text{reciprocal of diffusion length for thermal neutrons in iron, } 0.724 \text{ cm}^{-1} \]
\[ \mu = \text{total gamma absorption coefficient, cm}^{-1} \]
\[ \mu_1 = (1 + \alpha_1) \mu, \text{ cm}^{-1} \]
\[ \mu_2 = (1 + \alpha_2) \mu, \text{ cm}^{-1} \]
\[ \mu_e = \text{gamma energy absorption coefficient, cm}^{-1} \]
\[ \mu_s = \text{total gamma absorption coefficient in core, cm}^{-1} \]
\[ \nu = \text{Poisson ratio, dimensionless, 0.3} \]
\[ \Sigma_c = \text{macroscopic capture cross section, } 0.312 \times 10^{-3} \text{ cm}^{-1} \]
\[ \Sigma_f = \text{macroscopic fission cross section, } 1.79 \times 10^{-3} \text{ cm}^{-1} \]
\[ \sigma_{\text{p,max}} = \text{maximum tangential pressure stress, psi} \]
\[ \sigma_{\text{t,max}} = \text{maximum tangential thermal stress, psi} \]
\[ \phi = \text{neutron flux as a function of position, neutrons/cm}^2\text{-sec} \]
\[ \bar{\phi} = \text{average neutron flux, } 7.92 \times 10^{14} \text{ neutrons/cm}^2\text{-sec} \]

**TABLE XXI. Absorption Coefficients**

<table>
<thead>
<tr>
<th>( E_x, \text{MeV} )</th>
<th>Iron</th>
<th>Solution</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \mu, \text{cm}^{-1} )</td>
<td>0.464</td>
<td>0.244</td>
</tr>
<tr>
<td>( \mu_e, \text{cm}^{-1} )</td>
<td>0.204</td>
<td>0.185</td>
</tr>
</tbody>
</table>
APPENDIX C  INTERNAL RECOMBINATION CALCULATIONS

C.1 Design Criteria

<table>
<thead>
<tr>
<th>Power</th>
<th>80 Mw</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core capacity</td>
<td>172.5 lb-mol</td>
</tr>
<tr>
<td>Total system</td>
<td>750 lb-mol</td>
</tr>
<tr>
<td>Fuel concentration</td>
<td>12.8 g U(^{235})/l</td>
</tr>
<tr>
<td>Average core temperature</td>
<td>275°C</td>
</tr>
<tr>
<td>(\text{Cu}^{++})</td>
<td>0.02 m</td>
</tr>
</tbody>
</table>

C.2 Basic Realionships

Assume that the effect of excess \(\text{O}_2\) is to decrease the effective concentration of the \(\text{H}_2\) in the solution. With this assumption the recombination rate is not affected by the excess oxygen. Then the balance equation for the hydrogen concentration, \(x_{\text{H}_2}\), is given as

\[
\frac{dx_{\text{H}_2}}{dt} = v - k_{\text{Cu}}x_{\text{Cu}}x_{\text{H}_2},
\]

where \(x_{\text{H}_2}\) = \(\text{H}_2\) concentration, \(\text{lb-mol}\ \text{H}_2/\text{lb-mol solution}\)

\(x_{\text{Cu}}\) = \(\text{Cu}^{++}\) concentration, \(\text{g-mol}/\text{l}\)

\(v\) = gas production rate, \(\text{lb-mol H}_2/\text{lb-mol solution-sec}\)

\(k_{\text{Cu}}\) = copper recombination rate constant, \(1/\text{g-mol Cu}^{++}\)-sec

\(t\) = time, sec.

For steady operation,

\[
x_{\text{H}_2} = \frac{v}{k_{\text{Cu}}x_{\text{Cu}}}.
\]

The recombination constant may be expressed as
\[ k_{Cu} = ae^{-b/T} \]  
\[ \text{where } a = 2.37 \times 10^{10} \text{ l/g-mol Cu}^{++} \text{-sec} \]
\[ b = 12,200 ^{\circ}\text{K} \]
\[ T = \text{temperature of solution, } ^{\circ}\text{K}. \]

Henry's Law relates the partial pressure of the gas to its concentration in the solution as
\[ P_{H_2} = H x_{H_2}, \]
\[ \text{where } P_{H_2} = \text{partial pressure of H}_2, \text{ psi} \]
\[ H = \text{Henry's Law constant for H}_2, \text{ psi lb-mol solution}. \]

Combining Eqs. (C.2.3) and (C.2.4) gives a relation between the partial pressure of H\(_2\) and the minimum concentration of Cu\(^{++}\) for 100% recombination as
\[ P_{H_2} = \frac{Hx}{k_{Cu}x_{Cu}}, \]

Equation (C.2.5) is plotted in Fig. 42 where, at full power, \( v = 3.4 \times 10^{-5}, k_{Cu} = 5.082 \text{ at } 275^{\circ}\text{C}, \text{ and } H = 2.2 \times 10^{5} \).

In order to avert an explosion in the pressurizer, a copper concentration of 0.02 m was assumed. It is seen for this concentration that the maximum H\(_2\) partial pressure at the core exit is 74 psi or about 4% of the total pressure. Therefore, from the core exit to the pressurizer, the time delay is such that the partial pressure of hydrogen is reduced to below 1% of the total pressure as specified for the pressurizer.
Fig. 42 - Hydrogen Partial Pressure vs Cu$^{++}$ Concentration

TOTAL PRESSURE = 1750 psia
TEMPERATURE = 290°C
OF CORE EXIT
APPENDIX D PRESSURIZER HEAT BALANCE

D.1 Introduction

It is necessary to operate the pressurizer at about 300°C. The necessary calculations are presented here to show that it is feasible to utilize the decay heat for this purpose.

The sources of heat are 1) the heat of recombination and 2) the decay heat. The heat losses from the pressurizer are: 1) the heat removed in the purge condenser, 2) the sensible heat in raising the fluid temperature from 290 to 300°C and 3) the surface heat losses.

D.2 Heat of Recombination

Assume 20 gpm flow rate to the pressurizer and a hydrogen concentration of $3.35 \times 10^{-4}$ lb-mol H$_2$/lb-mol solution, the moles of hydrogen recombined in the pressurizer is then found to be $2.4 \times 10^{-3}$ lb-mol H$_2$/min. Assuming 100% recombination and 112,000 Btu/lb-mol, the amount of heat produced from the recombination of H$_2$ and O$_2$ is about 270 Btu/min.

D.3 Decay Heat

Glasstone (37) gives an expression for the total decay energy as

$$\Gamma(t) = 2.4 t^{-1.2}$$  \hspace{1cm} (D.3.1)

where $\Gamma(t)$ = the combined beta and gamma energy released, Mev/sec-fission

$t$ = time after fission, sec.

Using the following time scale,

- Startup
- Fission soup occurs
- Delayed soup leaves
- Core pressurizer enters
- Particles leaves
- Restart pressurizer

time, sec.
it is seen that
\[ \Gamma(t) = 2.4 (T-T_0)^{1.2} \text{ Mev/sec-fission.} \quad (D.3.2) \]

For a core power of 80 Mw and assuming 0.8 of the fission energy is deposited locally, the fission density is given as \( 2.96 \times 10^{16} \) fission/sec-ft\(^3\). Then it is seen that the energy released while the reactor is operating is simply
\[ \Gamma = 2.96 \times 10^{16} \times 2.4 \int_0^T (T-T_0)^{-1.2} dT \text{ Mev/sec-ft}^3. \quad (D.3.3) \]

Performing the integration and substituting in the limits, one obtains
\[ \Gamma = 3.55 \times 10^{17} \left[ (T-T_0)^{-0.2} - T^{-0.2} \right] \text{ Mev/sec-ft}^3. \quad (D.3.4) \]

Now integrate over the volume of the pressurizer to obtain all the energy emitted while the fluid is passing through the pressurizer as
\[ \Gamma = 3.55 \times 10^{17} \int_{\text{vol.}} \left[ (T-T_0)^{-0.2} - T^{-0.2} \right] dV \text{ Mev/sec} \quad (D.3.5) \]

where the pressurizer volume element is given by
\[ dV = Q \, d\tau \quad (D.3.6) \]

and \( Q \) = volumetric flow rate through pressurizer, ft\(^3\)/sec

\( d\tau \) = residence time of fluid in pressurizer, sec.

Substitute Eq. (D.3.6) into (D.3.5) and perform the integration over time \( \tau = \tau_1 \) to \( \tau = \tau_2 \) to obtain the amount of energy released in the pressurizer as
\[ \Gamma = 4.45 \times 10^{17} Q \left[ (T-T_0)^{0.8} - T^{0.8} \right] \tau_2^2 \text{ Mev/sec.} \quad (D.3.6) \]

Assume 20 gpm flow rate through the pressurizer at 290\(^\circ\)C, then \( Q = 0.0445 \text{ ft}^3 \). Also assume that the reactor has been operating several days so that \( \tau_1 = \tau_2 \) (in seconds) and \( \tau_1 - T_0 = 3 \) sec and \( \tau_2 - T_0 = 585 \) sec.
With these assumptions the rate of energy release in the pressurizer is given as
\[ r = 3.21 \times 10^{18} \text{ Mev/sec} = 29,300 \text{ Btu/min}. \]

Assume 10% of the energy is lost by escaping gamma rays, then the decay heat input to the pressurizer is 26,400 Btu/min. Adding this to the heat of recombination, the total heat input is found to be about 26,700 Btu/min.

D.4 Heat Loss in Purge Condenser

Assume that 39 lb/min of water vapor and gases are cooled from 572°F to 560°F and 6.7 lb of water are condensed at 560°F. The heat removed in this operation is about 5000 Btu/min.

D.5 Sensible Heat

About 3000 Btu/min are needed to raise the temperature of the 20 gpm stream going to the pressurizer from 290°C to 300°C.

D.6 Surface Heat Loss

The surface area of the pressurizer drum and standpipe is about 190 ft². Assume an ambient temperature of 140°F and an over-all heat transfer coefficient of 1 Btu/hr ft² °F. With these assumptions the heat loss to the surroundings is about 1375 Btu/min.

D.7 Heat Balance

The heat input to the pressurizer is about 26,700 Btu/min while the losses are 9,375 Btu/min. It is seen that about 17,300 Btu/min must be removed in order to keep the pressurizer at 300°C. This heat can be removed by placing a cooling jacket on the 12-in. pressurizer standpipe.

The cooling load may be lowered by redesigning the system so that a small flow is put through the pressurizer. This would result in a lower value of the decay heat which is the main contribution to the heating.
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