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OAK RIDGE SCHOOL OF REACTOR TECHNOLOGY

Reactor Design and Feasibility Problem

BOILING HOMOGENEOUS REACTOR FOR

POWER AND U^{233} PRODUCTION

OAK RIDGE NATIONAL LABORATORY
OPERATED BY
CARBIDE AND CARBON CHEMICALS COMPANY
A DIVISION OF UNION CARBIDE AND CARBON CORPORATION

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F. C. Vonderlage, Director

Reactor Design and Feasibility Problem

"BOILING HOMOGENEOUS REACTOR FOR POWER AND U²³³ PRODUCTION"

54561

H. R. Zeitlin, Group Chairman

H. W. Bertini

W. F. Bourgeois

J. N. Calvin

J. R. Engel

G. H. Farberman

O. R. Meyer

D. P. Ross

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For The Atomic Energy Commission

H. F. Canale

Chief, Declassification Branch TC

August 1954

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PREFACE

In September, 1953, a group of men representing various scientific and engineering fields embarked on the twelve months of study which culminated in this report. For nine of these months, formal classroom and student laboratory work occupied their time. At the end of that period these eight students were presented with a problem in reactor design.

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

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

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

F. C. VonderLage

for

The Faculty of ORSORT

ACKNOWLEDGMENT

We are very much indebted to Drs. P. C. Zmola and L. G. Alexander for their fine assistance and guidance throughout this project. We received the aid of many members of ORNL, in particular, Messrs. J. McBride and W. Patterson who cooperated and assisted in the laboratory study of the properties of mixed oxides, and Messrs. W. Cater, D. Ferguston, A. Kitzes, R. Lawson, S. Reed, D. Sasmor, C. Secoy, M. Silverman and R. Stoughton. We also wish to acknowledge the fine cooperation received from members of a concurrent ORSORT study group lead by Mr. H. Kamack.

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1.0 ABSTRACT

The design of a 500 megawatt single region boiling homogeneous reactor for central-station power, fueled with $\text{UO}_2\cdot\text{ThO}_2$ slurried in D_2O is reported, including an analysis of the problems of power removal and control. *all*

2.0 INTRODUCTION

The aqueous boiling system has for some time been an attractive competitor to the equivalent non-boiling system, some of the reasons being:

1. higher maximum reactor temperature for a given operating pressure.
2. pumps and other mechanical devices not required.
3. steam might ultimately pass directly to turbo-generator system.
4. more efficient heat transfer.

Such systems also have certain problems, in particular those of control and stability, and power removal, which may limit the feasibility of the boiler or make the costs prohibitive.

It has been the objective of this study to analyze the problems of control and power removal, to determine what limits the engineering and economic feasibility of such a system, and finally to develop a preliminary design for a typical boiling reactor system.

3.0 SUMMARY

A preliminary design of a 500 MW, one region, boiling homogeneous breeder reactor fueled with a slurry of $\text{ThO}_2 \cdot \text{UO}_2$ in D_2O operating at 1000 psia pressure, has been developed. The reactor is of the natural circulation type employing internal liquid vapor cyclone separators and an internal downcomer, and requires no pump or major mechanical devices in the primary loop.

The nuclear calculations are primarily based on a one region, homogeneous core model. The results of calculations using the actual non-homogeneous boiling model and of other calculations using two group-two region approximations to account for the existence of a riser and downcomer region indicate that the simple one region, homogeneous model (using the proper average void fraction) is sufficiently accurate.

The reactor has been made inherently safe by providing for negative partial reactivity coefficients of power and pressure by incorporating into the primary loop a large vapor (D_2O) storage volume. No mechanical control devices are employed, other than for long term fuel concentration adjustment.

A new chemical process, designed specifically for a slurry fuel in which U^{233} and Th need not be separated and high decontamination factors are not required, has been proposed to reduce fuel costs. The alternate would be a somewhat more costly modified Thorex process.

A crude cost estimate indicates that such a system may produce power for 7.7 mills/kwhr.

The general problems of power removal and control (stability) have been analyzed. A summary of the control analysis follows and a somewhat more detailed summary of the power removal problem will be found in section 5.2.1.

The control of boiling reactors divides into two phases, short-term stability and long-term stability.

Short-term stability concerns the statistical fluctuations in the vapor fraction which occur in a boiling system. If a decrease in vapor fraction occurs the reactivity of the core will tend to increase and the nuclear power will start to rise. If the rise in power produces more vapor without appreciable delay due to superheating the liquid the reactivity will be promptly reduced again to zero, i.e., the system is self-stabilizing. It is assumed throughout this report that previous investigations and experiments have demonstrated this to be so.

The second phase has to do with the vapor fraction averaged over times long with respect to the first phase but short with respect to thermal delay times, and with that this report has been concerned. The basic criterion used has been that fast reactivity controls operated by automatic control systems (such as servo-controlled poison rods or moderator removal) are not feasible in this system. The reasons are:

- (1) The flux and pressure information "noise" induced by the short term oscillations would be difficult to filter out of the control.
- (2) Poison rods are out due to their destruction of the breeding gain, and all other means also introduce moving parts into the control system.

- (3) Any such control system is liable to failure and, as such, becomes a menace in itself to integrity of the system.
- (4) Entirely apart from 1, 2, and 3 a reactor that is self-controlling is desirable from the operator's viewpoint, especially when one is seriously proposed for widespread central-station application.

With this basic criterion, an attempt was made to see if certain necessary stability conditions could be established:

- (1) That the partial coefficient of reactivity with respect to power (pressure, fuel inventory, D_2O inventory constant) be negative throughout a wide range of conditions.
- (2) That the partial coefficient of reactivity with respect to pressure (power, fuel, D_2O constant) be negative over a wide range of conditions.
- (3) That the operating curve of pressure vs power (obtained by postulating that the reactivity, D_2O , and fuel are constant) have a negative slope from 0% to 100% power, i.e., a "drooping" pressure characteristic, and that the pressure change not be too large, eg. 500 psi.

The study indicated that such a system could be built with a reasonable degree of practicability. It is hoped that the most important contribution, however, has been to establish these criteria and an indication of how the problem may be investigated more intensively. Certainly the hydraulic characteristics (vapor fraction and downcomer height vs thermal power removal at constant pressure, D_2O , and fuel inventory) are deserving of further study since they are the basis of

establishing the 3 criteria above. The entire basis of establishing the feasibility of a boiling homogeneous reactor lies in taking these hydraulic characteristics and from them calculating the corresponding core reactivities. Only then is it known whether the hydraulic characteristics and the criticality requirements can both be satisfied in a system meeting the basic stability criteria.

The next problem, after the above steady-state characteristics, is to write the transient relations so that they may be placed on an analog simulator and a detailed control design study performed.

With a system thus established, transient response curves may be taken directly off the simulator with a practical certainty that the response will be satisfactory, and thus there is no need to repeat laborious transient calculations on a wide variety of system designs.

4.0. SPECIFIC DESIGN STUDY

4.1 Study Philosophy and Design Basis

The integrity and engineering feasibility of a boiling system were given prime consideration over the economic feasibility, the feeling being that it is these features which will ultimately sell the reactor and that little if any improvement could be made on the costs of power being submitted by the various sponsors of different reactor systems.

We have consequently chosen for the specific design study a system which appears to have the best engineering possibilities. Such a system incorporates a one-region, slurry fuel reactor, operating at 500 MW of heat power; it has a natural circulation power removal system incorporating internal liquid-vapor separators and an internal down-comer. These design features have been justified as follows:

- (1) One region: A one region reactor eliminates (a) the need for two power removal systems, (b) the pressure differential hazard between (boiling) regions, (c) the fabrication problem involved in incorporating an efficient blanket around the core, and (d) the chemical processing required for a second region.
- (2) Slurry fuel: A $\text{UO}_2\cdot\text{ThO}_2$ solid fuel suspended in D_2O offers the following advantages over a UO_2SO_4 solution:
 - (a) Eliminates corrosion problem.
 - (b) Chemical processing may be simpler (see proposed processing scheme).
 - (c) Eliminates temperature limitation imposed on UO_2SO_4 (phase separation)

(d) UO_2SO_4 is reduced if sufficient O_2 is not maintained in the system.

(3) Pressure: An operating pressure of 1000 psi (design pressure 1350 psi) was assumed to be the limit as imposed by fabricability of a pressure vessel approximately 20' in diameter.

(4) Natural Circulation with Internal Liquid-vapor Separators and Internal Downcomer: Natural circulation eliminates the need for a pump. Internal vapor-separators and downcomer reduce the fabrication problems involved with the introduction of pipes in the pressure vessel. An internal downcomer also appears to reduce the overall cost by reducing fuel inventory per unit of power costs.

(5) Power: An upper limit of the power which can be removed from a core having a diameter of the order of 18 to 20 feet, as required for breeding, is approximately 500 megawatts as imposed by the capacity of the liquid-vapor separators and the number of these which can be placed in the pressure vessel.

(6) Breeding ratio: For the specific design a breeding ratio of 1 was arbitrarily chosen.

These design specifications and other pertinent reactor data are presented in Table 1.

TABLE 1REACTOR AND POWER PLANT DATA

Reactor Data:

Power, mw heat	500
mw electrical	150
Specific power, kw/gm U ²³³	1.48
kw/lb D ₂ O in reactor plant	2.75
Pressure, operating, psia	1000
design, psia	1350
Temperature, °F	545
Fuel	U ²³³ O ₂ ·ThO ₂
Moderator - coolant	D ₂ O
Pressure vessel	Hemispherical headed cylinder 20.5' OD x 50' high
Core	Right circular cylinder 17.5' dia x 17.5' high
Breeding ratio	1.03
Flux(average, thermal) neutrons/cm ² -sec	4.95 x 10 ¹³
Reactor materials inventory	
U ²³³ kg	339
Th kg	20,100
D ₂ O lbs	182,000
Average void fraction	32%

TABLE 1 (continued)

Power Plant Data:

Throttle steam pressure, psia	650
Throttle steam temperature (saturated), °F	495
Steam flow, lbs. per hr.	1,000,000
Turbine capability @ 1.5"	80,000 kw @ 3600 rpm
Net station efficiency	30.0%

4.2 Engineering Design

4.2.1 Reactor Plant

The reactor plant which consists of the reactor core system, the primary heat exchange system, and the control and fuel handling system, is illustrated schematically in Figure 1 and is described in detail in the following paragraphs.

a. Core

The core of the reactor shown in Figure 2 is enclosed in a cylindrical pressure shell with an inside diameter of 18.5 feet and a height of 50 feet. The active portion of the core is an approximate 17.5 foot diameter by 17.5 feet high right cylinder, consisting of a riser region 14.5 feet in diameter surrounded by a 1.5 feet thick annular downcomer region. A zirconium shell separates these two regions.

The boiling core mixture rises through the riser pipes and enters the steam separators. Here the vapor and liquid are separated with the liquid being returned to the active core through the downcomer. The vapor rises through the chevron steam driers, where

Figure 1

PRIMARY SYSTEM FLOW SHEET

DRNL-LR-DWG: 2548

entrained moisture is removed, and enters the steam dome where 2-36 inch -O.D. pipes take the steam to the external steam chests. Returning condensate from the heat exchangers is introduced at the bottom of the pressure vessel and flows upward through the thermal shield, discharging into the region above the steam separators. Since cooling the thermal shield might cause some boiling of the condensate, it was decided that upward flow eliminates any chance of "vapor lock", and at worst might cause some "percolating" in the thermal shield. The condensate is able to drain to the core through pipes provided in the separator region (see Figure 17). Fuel feed and concentration control is afforded through a system of concentric pipes located at the bottom of the core.

To agitate the slurry mixture when at zero power and sub-critical, steam is blown into the core region through small holes in a ring sparger located along the bottom of the reactor.

The pressure vessel is of all-welded construction made of mild steel internally clad with 347 stainless steel. The wall of the vessel is 9 to 12 inches thick, depending on the temperature distribution through the walls. This will be sufficient to withstand the design pressure of 1350 psi.

The height of the pressure vessel is determined by the control characteristics of the reactor. To provide for stable operation, and to obtain a system with negative power and pressure coefficients of reactivity, a large steam storage space is necessary. This volume takes the form of a large steam dome in the core pressure

FIG. 2 REACTOR VESSEL

vessel and two external steam chests, one each being located between the core pressure vessel and each of the two primary heat exchange loops. Should the stability of the reactor be such that the core pressure vessel steam dome volume can be located in the external steam chests, the core vessel height could be reduced correspondingly.

b. Primary Heat Exchange System

Heat Exchangers:

Since the reactor plant has been divided into two secondary loops (See Figure 1), the heat exchangers for each loop only carry one-half of the load. It is desired that each loop contain two heat exchangers, which are arbitrarily called HX-1 and HX-2. By placing these exchangers in series, the disassociated D_2O concentration is increased enough in HX-1 so that effective recombination can take place between the two exchangers.

The two heat exchangers are of similar design, differing only in size. Each one condenses half the flow, with recombination and steam drying taking place between the exchangers. They are each designed to remove 125 MW of heat. They are vertical, single pass, shell and tube type exchangers, with primary steam at 1000 psia being condensed on the shell-side and secondary water at 65-psia being boiled on the tube-side. The overall heat transfer coefficient has been taken to be 500 BTU/Hr.- FT^2 - $^{\circ}F$.

Since these heat exchangers operate in a natural circulation system, it is necessary to keep the pressure drops as low as

possible. It has been determined that in HX-1 the friction head loss on the primary side will be in the order of 1.5 feet of water, and the corresponding loss in HX-2 about 1 foot of water. On the secondary flow side, the friction pressure loss is 4.6 psi for HX-1 and 4.3 psi for HX-2. The primary side on HX-1 is fed by 2-24-in-OD pipes, and exhausts with one 24-in-OD pipe; HX-2 is fed by one 24-in-OD pipe, and exhausts with a 10-in-OD pipe. The secondary feed in both exchangers is by an 18-in-OD pipe, and the exit header is 24-in-OD.

The boiling secondary flow is conducted from the heat exchangers to a standard type of steam drum where the steam is separated and the feedwater introduced. Downcomers from the steam drum provides the feed for the secondary flow.

In order to separate the moisture condensed in HX-1, a "chevron" type steam drier (See Figure 3) is placed between the two exchangers. This has a frontal area of flow approximately 50 sq. ft., and effectively separates all the moisture from the flow with a very small pressure drop. What little moisture remains (less than 1%) is revaporized by the heat of the recombiner, which follows this drier in the loop.

FIG. 3 PRIMARY LOOP STEAM DRIER

Recombiners:

The gases formed by decomposition of D_2O in the reactor core must be recovered in a recombination system. Boiling in the core makes the use of an internal solution catalyst impractical because of the short contact period between gases and catalyst. Since it is desirable to effect most of the recombination at reactor pressure, platinum catalyst recombiners were selected.

The primary units (see Figure 4), which handle about 99% of the recombination load are located in the vapor line between the primary heat exchangers, one in each of the main loops (see Figure 1). The catalyst beds are cylindrical, 8 feet long, 24 inches in diameter, and 2 inches thick. The catalyst itself is 0.3% platinized alumina in 1/8 inch by 1/8 inch cylindrical pellets and is contained between stainless steel screens.

The unrecombined gases are bled off, along with fission product gases and some D_2O vapor, at the condensate return line from the second heat exchanger in each loop. These streams are merged, reduced in pressure and passed through a small, auxiliary recombiner. As much D_2O as possible is recovered by condensation and the remaining fission products enter the off-gas system.

Gas Production: According to a personal communication from C. H. Secoy, the rate of decomposition of D_2O , or G value, in mixed oxide slurries is about 0.4 molecules per 100 ev. Based on a reactor heat load of 500 MW, this results in a net decomposition

rate of 3290 lb/hr of D_2O . The heat available from recombining this amount of D_2O is 9.23×10^6 Btu/hour.

Design: The calculations for the catalyst bed design were based upon the design for the ISHR recombiner as reported in CF 53-6-162. The method of calculation was taken directly from this report with minor changes to meet existing conditions, and is summarized in the appendix.

Since heat is transferred from the primary fluid by condensation, it is undesirable to have large concentrations of noncondensable gases in the stream. Therefore, the recombiners were placed between the heat exchangers where this concentration is about 0.4 mole percent.

The flow rate through each unit is 6.5×10^5 lb/hr of saturated steam, 329 lb/hr of D_2 , and 1320 lb/hr of O_2 . Design calculations indicated a bed thickness of 1.45 inches for 99% recombination and the assumed flow area. This thickness was increased to 2 inches to allow for possible poisoning of the catalyst. The actual pressure drop through the bed is expected to be 0.39 psi.

Since platinum is a self starting catalyst only when dry, some provision must be made for applying heat to drive off any

moisture which may accumulate during shutdown. Once started, the heat from the recombination reaction will keep the bed dry. Probably the most convenient method of accomplishing the required heating will be to permanently install electric coils in the beds to be used as needed.

Utilization of the recombination heat for superheating the secondary steam was deemed undesirable because of the more complicated radioactive heat exchange system required and the small amount of superheat that can be obtained.

FIG. 4 CATALYTIC RECOMBINER

0371228.0330

c. Operation and Control

The following suggested control for a 500 MW, 17.5 x 17.5 nominal nuclear core size reactor is given. (See Figure 5.)

It is assumed that the electrical output of the plant will vary from 80-100% of design rating with short daily shutdowns in power to much lower levels.

A steam dumping condenser which is a high pressure, high temperature condenser on the secondary steam line controlled by secondary steam pressure or flow, is provided. This is more than ample for dissipation of decay heat, but its primary purpose is to maintain approximately constant steam flow while the electrical load varies by $\pm 10\%$. In addition, for the cases of complete loss of electrical load or loss of both TG sets it will maintain the core at 125 MW to prevent the slurry from settling and the necessity for complete shutdown of the reactor.

Two primary side steam chests of 15 feet I.D. x 22 feet long are next specified on the basis that a complete loss of secondary cooling would cause the primary steam pressure to rise to 1350 psi (neglecting decay heat) and that the reactor operating under constant D_2O and fuel inventory has negative coefficients of reactivity vs. power and pressure. The total steam volume thus specified is 16,000 cubic feet. A decrease in load under these conditions from 100% to 25% will increase the pressure from 1000 psi to 1150 psi without any necessity for external controlling action.

Since, with the secondary steam dump condenser, a complete loss of cooling to the core is very unlikely, the pressure relief valves are set at about 1400 psi. A backup rupture disk of 1800 psi is also installed, but the design pressure for the primary system is 1350 psi.

Although it is always possible that in a nuclear power system, involving as it must energy storage elements, unstable or oscillatory operation may occur, no inherent instability features were discovered in the system specified. Therefore, it should be possible by further investigation with the aid of an analog simulator to specify the exact design of the plant components so that stable operation is ensured without the attempted addition of automatic reactivity controls. It was intended that the control system here shown be conservative in this respect so that an upper limit on sizes would be established.

For the purposes of setting the breeding gain, chemical processing, and startup and shutdown, a primary feed and let-down system is specified. (See Figure 1 and 6.) This is primarily a trimming device utilized during power operation to permit setting the pressure closely without dumping unnecessary steam, and is designed as follows.

During operation, slurry is continuously pumped through an external loop at 100 gpm. After cooling in a 750 KW shell-and-tube heat exchanger, the slurry from the reactor enters a small tank for measurement of concentration. The principal

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SUGGESTED CONTROL SYSTEM FOR U²³³-TH-D₂O REACTOR
WITH NATURAL CIRCULATION BOILING
500 MW
17.5 x 17.5 ft CORE

Fig. 2

purpose of the cooling is to prevent cavitation of the pump. Another tank is provided in the main loop on the discharge side of the pump to permit addition and/or removal of slurry.

Slurry removal is governed primarily by chemical processing requirements. The recommended cycle of 334 days handles material at the rate of approximately 100 gallons per day. This amount is withdrawn all at once each day and requires about five minutes. After cooling to 150°F, the slurry is let down to atmospheric pressure through a throttling valve and dropped into a stilling tank from which it is sent to chemical processing. This tank is equipped with cooling coils to remove decay heat.

Decontaminated material is returned from the chemical processing plant in two streams, pure D₂O and concentrated slurry at 1000 gm/l. These streams are stored in 600 gallon tanks for addition to the core as required. The slurry tank is mechanically agitated to prevent settling. The capacity of these tanks is sufficient to provide fuel and D₂O for full load operation of the power plant for about 10 days in the event of breakdown in the processing plant. Slurry addition to the main circulation loop is accomplished by means of two 50 gpm pumps.

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FIG.6 SLURRY CONCENTRATION CONTROL BY PUMPS

A series of agitated, sub-critical dump tanks with a total capacity of 55,000 gallons is provided for shut down. Slurry is dumped into these tanks from the stilling tank. Cooling is provided to remove decay heat and a small line to the off-gas system permits the discharge of gases.

At startup, material from the dump tanks is pumped through thickeners into the 600 gallon storage tanks from which a critical mixture is fed into the core. The thickeners may also be used for the hot slurry if it is desirable to send two streams to chemical processing.

Due to the questionable reliability of pumps, an alternative concentration control system utilizing compressed gas to transfer the fluid streams (see Figure 7), was examined. This system eliminates all but the 100 gpm circulating pump, as well as the slurry throttling valve. However, the large volumes of radioactive gas involved tend to make such a system undesirable.

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SLURRY CONCENTRATION CONTROL

ORNL-LR-Dwg. 7287

Fig. 7
-33-

Start-up and Shutdown:

Due to the indicated negative temperature and power coefficients of reactivity of the reactor design (see Section 5.3.2) no hazards are involved in the start-up of the reactor. The method is to pump in the slurry at the critical concentration maintaining it at saturation temperature in the core. When a volume in excess of critical is reached, the core nuclear power increases to a level where the bubbles formed by boiling have introduced a negative reactivity equal to the original excess. Since 1 Mw of power produces bubbles cancelling the excess reactivity introduced by 1 ft³ of slurry (approximately), the critical volume can be exceeded by 500 ft³ before design power would be reached.

Time to reach critical at 100 gpm = 5 hrs.

Time to reach 500 Mw after reaching critical = 40 min.

The principal problem of start-up is to keep the slurry from settling in the core before criticality is reached. The suggested method here is to introduce D₂O steam at several places in the bottom of the core which will keep the core boiling at a rate giving a volume of steam per minute equal to the core volume. This requires a 2500 Kw auxiliary boiler at 100 to 200 psi, chemically fired. The D₂O steam is condensed in one of the HX, drawn off by an auxiliary line and pumped back to the boiler with an auxiliary boiler feed pump.

This equipment is only used during start-up and shut-down and does not detract from the overall reliability of the main power plant.

Shut-down is a reverse process. Slurry is let down from the core at approximately 100 gpm or less. This reduces the operating power and pressure of the core until the nuclear power is zero and the operating pressure is slightly above atmospheric. Decay heat will keep the core boiling for a short time thereafter, but the auxiliary D₂O steam must again be used to keep the slurry in suspension while the remainder of the slurry is withdrawn. The total time required is about 6 hours.

In normal start-up and shut-down the secondary steam must be disposed of as required by the above procedures. This can be done with aid of the standby steam dumping condenser of 25% of full power capacity.

Reductions in power to 25% of full power (125 Mw) are not shut-downs and can be done at any time by reducing secondary steam load to that amount, the pressure not exceeding 1200 psi. Please refer to Figure 25.

Emergency shut-downs to no secondary cooling can be done resulting in a pressure rise to approximately 1350 psi. Of course, some cooling to provide for decay heat is needed at all times which is a second function of the standby steam dumping condenser. Note that the sample calculation in 5.3.2 is for 40% vapor and (a) 10,000 ft³ steam volume,

15' x 15' core and (b) 20,000 ft³ steam volume, 20' x 20' core. The design here given uses 16,000 ft³ which was fixed by trial and error to give stability during natural circulation boiling and is adequate for keeping the pressure below 1350 psi for complete loss of cooling (i.e. complete loss of bubbles).

4.2.2. Turbogenerator Plant

Heat from the reactor is furnished to the turbines by means of saturated steam at 650 psia. Each of the two turbogenerator sets is fed with approximately one million pounds of steam per hour, which is generated in the two heat exchangers connected in each loop. This steam is passed through one high pressure turbine, and then through a triple exhaust low pressure end, with condensation occurring at 1.5" Hg. abs. Steam is extracted at three places for feedwater heating. In order to keep the moisture content of the steam down to tolerance levels for erosion of the blading, which is 15% according to "Steam Turbines", Power, December, 1945, the low quality steam which is exhausted from the high pressure turbine is passed through a "chevron" type steam drier before being reintroduced to the low pressure sections. The moisture which is removed in the drier is introduced into the open feedwater heater.

Arbitrarily, three stages of feedwater heating were used in this analysis, with a 50F. terminal temperature difference considered in the closed heaters. A 5% pressure drop was assumed in the turbine steam extraction lines.

For the cycle indicated in Figure 8, which is based on a moisture content limit of 15%, three stages of feedwater heating, and an 80 MW, 3600 RPM turbogenerator, the gross plant efficiency is 32.4%. The net power plant efficiency, assuming 6% of the power produced is consumed in operating the plant auxiliaries, is 30.5%. The net station efficiency, which also considers power needed to operate the reactor, is 30.0%. Therefore, for our 500 MW heat generation, we will be able to sell 150,000 KW.

FIG. 8 STEAM CYCLE FLOW SHEET

4.2.3. Plant Layout and Building

A sketch of one possible plant layout is presented in Figure 9 to illustrate the relative position of various pieces of equipment and radioactive cells. Although not indicated by such a drawing, it appears that this system requires less piping, smaller heat exchangers, fewer components, and less shielded building space than does the equivalent non-boiling system, in addition to the advantage that no pump is required. The control area, offices, maintenance shops, etc. (not indicated) could be located along one face of the cell area building while an integrated chemical processing plant (not indicated) could be located adequately on the opposite face of the cell building.

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- LEGEND -

PV - PRESSURE VESSEL
SD - STEAM DRUM
HX - HEAT EXCHANGER

- SCALE -

1" = 40'

FIG. 9 PLANT LAYOUT - PLAN VIEW

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- SCALE -

1-in = 40-ft

- LEGEND -

PV - PRESSURE VESSEL
 SD-1 - PRIMARY STEAM DRUM
 SD-2 - SECONDARY STEAM DRUM
 HX - HEAT EXCHANGER
 RC - RECOMBINER
 MR - MOISTURE REMOVER

FIG. 10 PLANT LAYOUT - ELEVATION

4.3 Nuclear Calculations Summary

Critical concentrations were calculated by homogeneous one-group calculations for various sized cylindrical 1000 MW and 500 MW reactors and for different values of void fraction using breeding ratio as a parameter (see Appendix I, part 1, and Figures 29, 30, 31, and 34) These calculations were performed to determine the feasibility of the various systems considered. Core size and breeding ratio were varied to allow for preliminary economic evaluation and the void fraction was varied since at the time the proper void fraction based on hydrodynamic calculations was not known.

The flux and density distributions were determined for a one-region reactor whose void fraction varied along the height of the cylinder but remained constant radially (see Appendix I, parts 2 and 3, and Figure 38). The critical concentrations obtained from this variable density system were about equal to that from a one-group homogeneous calculation made on this variable density system.

The actual reactor is a two region reactor consisting of a riser and downcomer. The density in the riser is much less than that in the downcomer. The homogeneous calculations were made using an average void fraction throughout the entire reactor. Therefore to check the validity of the one-group homogeneous calculations two-group two-region calculations were performed for three cases (see Appendix I, part 5).

The results are listed below:

REACTOR SIZE	Th. CONC. Gms./liter D ₂ O	CRITICAL CONCENTRATION	
		Gms. 23/ liter D ₂ O	
		One-group	Two-group Two-region
15'h.x15' dia.	700	22.4	24.5
17½'h.x17½' dia.	400	7.60	7.82
17½'h.x17½' dia.	250	4.23	4.47

The results indicate that the one-group homogeneous calculations can be used with a fair degree of accuracy in these particular reactors provided that the average volumetric void fraction of the two region-system is used (see Appendix I, part 4, "Single Region").

An outline of the calculations is given in the appendix of this report.

5.0 TECHNOLOGY OF AQUEOUS HOMOGENEOUS BOILING REACTORS

5.1 Fuel Systems

5.1.1 Selection of Fuel and Fertile Material

All possible aqueous uranium systems were individually evaluated in terms of neutron characteristics, corrosion, thermal and radiation stability, and overall reactor design feasibility.

Uranyl fluoride and phosphate were eliminated because of the severe corrosion problem due to hydrolysis and the likelihood of precipitation of UO_3 .⁽¹⁾

The nitrate was considered unsatisfactory for a number of reasons. Both uranyl and thorium nitrate are thermally unstable at 250°C ; their radiation stability is unknown and the N^{15} isotope would have to be used.⁽¹⁾ Besides initial cost, the latter point makes chemical processing extremely difficult since the $\text{N}^{15}\text{O}_3^-$ ion must be replaced before HNO_3 dissolution and substituted back after processing.'

The selection of a fuel was thus limited to a choice between UO_3SO_4 and an oxide slurry, with a thorium oxide slurry being the fertile material in both cases. This choice of fuel also involved the choice of a basic reactor design i.e., a single or double region machine using UO_2SO_4 and/or an oxide slurry. The merits of these two reactor types have been evaluated in Section 4.1. The use of a slurry and solution combined in a one region reactor or two slurries in a two region reactor was considered unfeasible and undesirable respectively. The former presented the problem of UO_2^{+4} absorption on ThO_2 with subsequent reduction to UO_2 and precipitation.⁽¹⁾

(1) Reactor Handbook Volume II.

The latter alternative presents all the combined disadvantages of a one region slurry system and a two region reactor.

Having considered the merits of the UO_2SO_4 and the slurry systems, a slurry was finally chosen. To improve the homogeneity of the system a "mixed" oxide of ThO_2 and UO_2 was selected, in which the UO_2 is initially physically incorporated in the ThO_2 particles. Once the reactor has become a breeder, all of the U will be generated in the ThO_2 lattice.

The slurry is, of course, not considered an ideal fuel. It's chemical, physical, and engineering characteristics along with the limitations they impose will be listed in the following sections.

5.1.2 Slurry Technology

The present state of slurry technology is one of experimentation and development with no data on a complete engineering system available. The work being done at present is divided between two groups - oxide chemistry and oxide engineering.

1. Oxide Chemistry - all work is done on a laboratory scale.

Efforts have been concentrated on different methods of preparing ThO_2 , since the particle size and abrasiveness vary considerably with method of preparation and are in particular a function of the temperature of calcination.

Also since all ThO_2 preparations use $\text{Th}(\text{NO}_3)_4$ as a source material, the amount of nitrate impurity has been of interest although the effect of nitrate impurities on pH, corrosion, particle growth, and general slurry properties has not been fully evaluated. The high solubility of $\text{Th}(\text{NO}_3)_4$ at 250°C and its significant hydrolysis at this temperature could cause very

(1) A. Kitzes - Personal communication

undesirable results, such as possible crystal growth.

The different slurry preparations successful to date are listed in Table II along with existing data on each preparation.

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TABLE II
ThO₂ PREPARATION ⁽¹⁾

TYPE	CONDITIONS	PARTICLE SIZE	OTHER PROPERTIES
Thermal	1. Th(C ₂ O ₄) heated at 650°C	> .1 μ	1. 10 ppm nitrate; most abrasive of all laboratory preparation*
Decomposition	2. Th(C ₂ O ₄) heated at 400°C then at 600°C	0.1 μ	2. 4000 ppm nitrate
	3. Autoclave 2. in H ₂ O	--	3. Pronounced tendency to pack
	4. Reheating 3. at 600°C		4. Removed all tendency to pack, 200 ppm of nitrate
	5. Th(HCO ₂) ₄ heated at 500°C		5. Less abrasive than (C ₂ O ₄) ₂ calcined at same temperature.
Pyrohydrolysis	1. Th(C ₂ O ₄) - H ₂ O at 250°C	100 to 1000 A	1. Contaminated with organic, removed by calcination at 250°C ThO ₂ slurry in excess of 1000 g/l readily prepared
	2. Reautoclaving in H ₂ O at 250°C	-	2. Slow settling, readily dispersible slurry showing no tendency to pack
Hydrolytic Decomposition	Formate stripped by steam	--	produces hydrous oxide
Hydroxide	(C HO ₂) ⁻ ppt from (NO ₃) ⁻ solution dissolve in H ₂ O ppt (OH) ⁻ with NH ₄ (OH) calcine at 500°C	1 μ	Free of nitrate; 50 ppm doesn't pack on settling - conc up to 1500 g/l - might become viscous after pumping Settled vol 300-600 g/l. Least abrasive of all preparations*

(1) Personal Memo from J. McBride

Work is also being carried out on the stabilizing of ThO_2 slurries, with regard to settling, by the addition of an electrolyte. The addition of the pyrophosphate radical has produced slurries which show no tendency to settle over a period of days.

At present there is no laboratory data on the effects of radiation of ThO_2 slurries. This work will be carried out in the next few months.

2. Oxide engineering⁽¹⁾ - dynamic testing on a much larger scale than laboratory.

Three types of ThO_2 have been used. These, along with available properties, are listed in Table III.

TABLE III

Type	Preparation	Particle size (a)	Corrosion (b)
Ames Oxide	Thermal decomposition of oxalate at 900°C. - batch process	Most less than 200 Å, all < 0.1 μ	Initial rate less than Lindsay
Lindsay Oxide	Thermal decomposition of oxalate at 680° C. - continuous process	" "	Greatest initial rate
Material prepared by test group	Formate preparations Formate $\rightarrow$ Oxalate Cal. at 600 - 800° C.	" "	Initial rate less than Ames

(a) Particle size and settling rates^(c) at room temperature seem to be independent of material used.

(b) When ph of system reaches 5.2 all the corrosion rates proceed equally.

(1) Personal memo from A. Kitzes.

(c) Settling rate varies with concentration increasing with decreasing concentration of ThO_2 .

The corrosion listed in Table III is the corrosion behavior without an inhibitor. When O_2 is added to the system no corrosion of stainless steel was observed after 1500 hrs. of continuous operation.

The problem of keeping O_2 in a boiling slurry system is much different than in a boiling solution. Because of the high surface activity of ThO_2 , it is felt that sufficient O_2 will absorb on the surface to prevent corrosion. However, the exact mechanism involved is not completely understood. Should this addition of O_2 be impossible in a boiling system a titanium plumbing system would be necessary for the primary loop.

Both Zr and Ti show no corrosion or abrasion attack by ThO_2 . Flow velocities used in corrosion and abrasion tests range from 20 to 125 ft/sec. with the attacks on S. S. in the absence of O_2 increasing with increasing velocity.

3. Mixed oxides of Th and U⁽¹⁾

A. The available data on mixed oxides consist of several methods of preparation and the amount of uranium leached from these preparations - see Table IV.

However, since the mixed oxides of interest will be ones with very low uranium concentrations from 1 - 5%, the behavior of such slurries will be quite similar to that of the ThO_2 slurry; this is the reason for the extensive treatment of ThO_2 slurries given above.

(1) Personal Memo from S. Reed and J. McBride

TABLE IV

Acid Leach of Thorium -- Uranium Oxides

ThO ₂ Preparation	Final Calcination Temperature (°C)	% uranium dissolved with 8 N nitric Acid	
		First Treatment	Second Treatment
Oxalate calcination	300	7.2	0.04
Oxalate pyrohydrolysis (250°C)	350	9.6	0.06
Hydroxide calcination	500	12.4	0.06
Oxalate calcination	700	14.4	0.04
Pyrohydrolysis (250°C)	900	72	7.5

B. Preparation of mixed oxides

- i. Co-Precipitation of oxalate salts and calcination at 600° (must keep U in 4 state)
- ii. Co-crystallization of nitrates - evaporate to crystallize, fire at 600° C for 3 hours.
- iii. Add formic acid to nitrates - calcine at 600°C
- iv. Add $\text{UO}_2(\text{NO}_3)_2$ to ThO_2 - calcine at 600°C.
- v. Add ThO_2 to $\text{UO}_3 \cdot \text{H}_2\text{O}$ plotted form - heat at 200°C in H_2O .*

5.2 Power Removal

5.2.1 Core Hydrodynamics (See Appendix II for complete analysis and discussion)

The object of the hydrodynamic investigation was to develop relationships between representative core mixture densities and available power densities.

To this end, three models were evolved, the first postulating no slip between liquid and vapor as the mixture moves by natural circulation; the second involving constant slip; and the third postulating variable slip, as affected by bubble size. For simplicity the first case was followed to a conclusion.

* An X-ray diffraction pattern of a mixed oxide prepared in this manner, with 8% uranium, showed no indication of a separate uranium phase; presumably the uranium present was in solid solution.

In relating mixture density change to heat generation, axially, account was taken of the interacting effects of density variation (due to vapor build-up as the mixture circulates) on heat generation (which, in general, is dependent on mixture density multiplied by flux); this required difference solutions to the diffusion and density equations and resulted in a parabolic dependence of density on integrated heat generation. Radially, the vapor was assumed, because of turbulence, to spread out evenly over the cross section of the region (riser or downcomer) concerned. Thus a Bessel function was chosen for the radial heat generation form, and the axial form was merely averaged over the cross-section of the region (riser or downcomer) in question so as to eliminate the radial dependence. Incidentally, axially, the integrated heat generation distribution was turned end-for end in the riser as opposed to the downcomer so as to give what would seem a proper density form, despite possible effects on the radial flux distribution. The final result was to plot various characteristic mixture densities (average, riser, downcomer densities, etc.) against mean density decrease (see Figure 10).

Once the mixture density situation was analyzed, the fluid mechanical aspects of the system could be studied. By conservation of mass and energy and equating driving head to head losses, it was possible to set up relations between available power density and the various characteristic mixture densities previously obtained.

The information could be evaluated in two ways, inasmuch as the separator region flow losses are an uncertain estimation in

such a system. Either the separator losses could be set up as an analytical quantity; that is, in terms of a velocity head times a constant K which must be estimated, and curves made of power density vs. mean density decrease with reactor height as a parameter, or the power density and height could be fixed, and the curves of the driving head and head loss (leaving separator region losses undefined) vs. mean density decrease could be drawn.

However, inasmuch as there was also the problem of determining the optimum ratio of riser to total area for maximum power density, it became necessary to define the separator loss, hoping that the choice was such as not to invalidate the obtained result should the desired head loss be different. The resulting optimum area ratio was roughly .7 (see Figure 13), whereupon, for interest's sake, the power density vs. mean density decrease curve was drawn (see Figure 11). When the reactor power and size were fixed, the power density became fixed, as did the mean density decrease. The actual separator pressure drop was then calculated and found to be too low. It became necessary, then, to draw up the head characteristic curves and to find what mean density decrease would give the desired separator loss (see Figure 12). Although not investigated thoroughly for lack of time, it is believed that the area ratio of .7 was not affected appreciably within the limits of accuracy of previous work.

The final results gave an area ratio of .7, power density of 4.23 Kw/liter at a mean density decrease of .304 with a separator loss of 2 psi for the design reactor of 500 MW and 17.5' x 17.5' nominal core size.

Side Studies

(1) Because of the adaptability of the developed equations to a reactor with external downcomers, a comparison was made between such a system and the internal downcomer system. The comparison was made on the basis of the same total power output and the same void fraction in the riser of the external downcomer reactor as in the core of the internal downcomer reactor (where the core in the latter includes the downcomer). The analysis seemed to show that, on this basis, the internal downcomer system was slightly the better from the viewpoint of inventory charge per unit of power. However, the results are not necessarily conclusive because of the small difference (10%) and the possibility that the two parameters used were perhaps not the best ones to fix.

(2) An attempt was made to solve the density equation which postulated constant slip velocity. The velocity chosen was 1 fps, which according to P. C. Zmola seemed reasonable at the operating pressure and temperature. Inasmuch as the equation assumed negligible fuel weight, the result obtained may not be too applicable to the present case where the fuel concentration is roughly 250 g/liter. However, with that assumption, the effect of slip velocity on the power density was negligible. Incidentally, it perhaps should be pointed out that, when no slip is assumed, the fuel weight does not matter except in actual pressure drop calculations because of the direct variation of fuel density with mixture density. When slip is postulated, however, there is a differential effect of fuel density on fluid velocity as opposed to

vapor velocity, which in time affects the physical picture and the calculations.

Suggested investigations:

Inasmuch as the uncertainty of the results obtained depends mainly upon the flow loss assumptions, it is suggested that this be investigated more thoroughly through experiment. In particular, a mockup of the separator region should be made and a water-steam mixture passed through so as to fix more definitely the pressure drop through this reactor component.

Analytically, the expressions developed postulating slip velocity should be scrutinized, improved if possible, and solved to discover what the effect of slip really is.

Finally, a study should be made of the possible advantage in having external separators above the reactor core, with internal and external downcomers.

Since considerable driving head is obtained by merely increasing the vertical distance between the boiling liquid level and the separator region at the expense of larger S. F. material inventory, the power removal should be investigated as a function of this distance (and a feasible engineering design) for internally and externally located separator region.

Figure 11

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Figure 12

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Fig. 13

Fig. 14

5.2.2 Steam Separators

The basis upon which this study was made was that the vapor would be separated from the liquid in cyclone separators, and that these separators would be located in the pressure vessel which contains the active core. Such a system, it was believed, would be simpler to fabricate and less costly in development, since standard separators would be used. However, it was discovered that a very severe limitation was imposed on the system, insofar as power removal was concerned, due to the limited space in which to place the cyclones, and the lack of sufficient information about capacities and pressure drops through them. However, in order to provide a basis for future studies, internal vapor separators were incorporated in the design, with the result that the reactor power was limited to approximately 650 Mw in a pressure vessel less than 20' in diameter.

The data upon which the separator design is based is as follows:

2,620,000 pounds of vapor per hour

Liquid / vapor weight fraction 25 to 1

Assumed vapor specific volume 0.435 ft³/lb

Assumed liquid specific volume 0.159 ft³/lb

Homogeneous flow of slurry in water

First, since the capacities to be handled are so large, the number of separators required was examined. From curves and data provided by the Babcock and Wilcox Company (ORNL CF-52-7-74), it was shown that a large number of relatively small separators was

most feasible because of the high liquid quantities to be handled and the characteristics of cyclone separators of this type. It was decided to use 97 standard 14" cyclone separators in the reactor, each one handling 26,500 pounds of vapor per hour.

Since the number of separators required appears to be in the order of 100, and the capacities are very large, the matter of piping becomes important. In order to keep the pressure drop within the limits set by the demands of natural circulation, which is 2 psi, a flow area of approximately 75 ft² is required, which if it had to be of any considerable length would give a tremendous "hold-up" of the liquid. This indicated immediately that an arrangement of external separators might be undesirable.

Concerning the matter of liquid hold-up in the separating equipment, most of this will occur in the mixture chamber from which the separators draw the fluid. When cyclone separators are operating properly, there is only a thin film of liquid over most of the internal surfaces, and this might be in the order of 1/16 to 1/4 inch thick. Therefore, only about 1/10 to 1/5 of a cubic foot of liquid would be expected inside each separator.

Figures 15 and 16 show the arrangement of 97 separators in a 17.5 foot vessel. In this arrangement, the rising mixture passes through pipes to a chamber surrounding the separators. From this common mixture space, the mixture passes through the separators in parallel. The vapor flows upward into an upper chamber where chevron steam driers remove the entrained liquid from the vapor and from which connections are provided to take off the vapor to

external equipment. The separated liquid is collected between two plates below the separators and travels radially outward to an annulus along the shell of the pressure vessel which acts as a downcomer in the natural circulation system. Figure 15 shows 14" pipes, nested between the separators, which act as openings to permit the mixture to reach the separator inlet chamber. The upper plate of the inlet chamber is pierced by eight 8" pipes which provide sufficient flow area for the drain from the steam driers and for the condensate return, the latter being introduced to this plate after functioning as a coolant for the thermal shield.

Future work on this problem should include detailed studies of capacities and pressure drops of various sizes of separators, possible new designs of a separator which would have better characteristics than present units, and a study of the possibility of using separators external to the pressure vessel. The last proposal is probably the most promising of all. However, some of its major objections, at present, are large hold-up of liquid, fabrication of a pressure vessel with many holes, and lack of sufficient information on the performance of such a device.

FIG.15 14-in STD. CYCLONE SEPARATORS INSIDE
PRESSURE VESSEL ARRANGED FOR
NATURAL CIRCULATION

S - STEAM SEPARATOR
R - MIXTURE RISER PIPE
D - CONDENSATE DOWNCOMER PIPE

FIG. 16 PLAN VIEW OF SEPARATOR LAYOUT

5.3 Control

5.3.1 Control of a Natural Rise (Free Surface) Boiling Reactor

The most fundamental effect is that due to varying power removal rates from the core. To the first approximation the power removal rate may be assumed directly proportional to the vapor fraction f , where $f = \text{volume of vapor} / (\text{volume of vapor} + \text{volume of liquid})$. Since we are dealing with vapor fractions as high as 40% the effect of temperature on liquid density and cross-sections may be neglected in comparison. In addition, during power operation, the temperature of the core will remain approximately constant.

For comparative purposes we will assume a core geometry as follows:

FIGURE 17

An increase in vapor fraction will increase the height "h" and thus decrease β^2 . It will also increase M^2 by making the average distance between fuel and moderator atoms larger and this will be the overpowering effect. (see Appendix III, part 1) Thus the reactivity of the core must be increased for increasing power and the power may be plotted against Δk , the excess reactivity added over that required for zero power critical. (See Figure 18)

FIGURE 18

This is the most important single characteristic of this boiling reactor; that at a constant boiling temperature (or pressure) the power of the core is a direct function of the reactivity. Due to the rapidity of bubble formation it may be assumed for this study that there is a negligible time lag between power and reactivity and that very rapid oscillations of power may occur about the average value.

The change in vapor fraction can be compensated for most rapidly by changes in the volume of slurry (ThO_2 and UO_2 and D_2O) in the core or by changes in the volume of only D_2O in the core. As the vapor fraction increases either slurry or D_2O may be added to maintain criticality or, more exactly, either slurry or D_2O must be added to increase the power of the core. Conversely if the power of the reactor is to be decreased either slurry or D_2O must be removed from the core.

Additions of slurry of the same concentration increase the reactivity by decreasing B^2 and have no effect on k_∞ ($k_\infty = \eta \epsilon f p$) or on M^2 .

Additions of D_2O however increase the reactivity by decreasing B^2 and by increasing p , the resonance escape probability which is primarily a function of y , the Th concentration. It also tends to increase M^2 by increasing L^2 but this is over-ridden by the changes in p , so that D_2O addition is relatively more effective than slurry removal (see Appendix III, part 1).

A representative problem for purposes of comparison is:

- (a) Core operating at 40% vapor
- (b) How much D_2O or slurry must be removed to bring the vapor fraction to zero, i.e., reduce the power to zero.

Case I: (a) 20^{ft} I.D. x 20^{ft} high core @ 40% vapor

- (b) reduction of vapor fraction to zero would give a $k = 1.045$

Case II: (a) 15^{ft} I.D. x 15^{ft} high core @ 40% vapor
 (b) reduction of vapor fraction to zero would
 give a $k = 1.055$

	<u>Volume of Slurry Removed</u>	<u>Volume of D₂O Removed</u>
Case I	1500 ft ³	800 ft ³
Case II	644 ft ³	275 ft ³

METHODS OF EFFECTING REMOVAL

A pressurizer could be used to effect an inherent control by slurry removal. An enlarged primary steam space (steam drum) could be used to effect an inherent control by D₂O removal.

Sizes of the two types of equipment were estimated. For the pressurizer calculation the effect of the existing steam chest was not considered:

Core size at 40% vapor = 15' I.D. x 15'

Pressure vessel size = 15 I.D. x 40' high with one hemi-spherical end cap

Steam volume in PV = 3450 ft³

External steam volume in piping and HX = 2550 ft³

Total steam volume = 6000 ft³

Note that operation of both is similar in that as the power removal from the heat exchangers is decreased the excess of core power builds up the primary pressure. This increase of primary pressure forces slurry into the pressurizer in one case or removes D₂O into the steam chest in the other. One important difference is that the slurry must be accelerated

and moved into the pressurizer while an increase in the fraction of D_2O in the steam chest is a natural consequence of the reduction in condensation rate in the HX.

See Figure 19 and 20.

Therefore although the steady-state operations of both pressurizer and steam chest are quite similar the steam chest would have a much larger stabilizing effect than the pressurizer. The size of the steam chest is also an economic balance between the cost of the steam chest and the increased cost of the higher pressure designed pressure vessels and it is possible that no additional drums would be necessary, the inherent space within the pressure vessel, piping, and HX being sufficient.

No mechanisms operating within the primary system are necessary for either pressurizer or steam drum. The steam drum actually requires no mechanisms or power of any kind.

(See Appendix III, part 2).

FIGURE 19

Pressurizer vs. Steam Chest Control

15' x 15' Reactor Core

	<u>Pressurizer</u>	<u>Steam Chest</u>
1. Final Pressure	1500 psia	1500 psia
2. Surge Pressure	~ 2000 psia	1500 psia
3. Size	12' I.D. (3530 ft ³)	2 @ 10' I.D. (4000 ft ³)
4. Hold-up (D ₂ O)	59,000# \$2.95 x 10 ⁶	9000 # \$0.45 x 10 ⁶
5. Hold-up (Th)	32,000#	-----
6. Hold-up (U ²³³)	585 # 2.66 x 10 ⁵ gm 6.65 million dollars	----- ----- -----
7. Criticality problems	yes	no
8. Slurry settling problems	yes	no
9. Piping connection problems	yes	some

FIG. 20- POSSIBLE STEAM CHEST CONFIGURATION

Secondary Steam Dumping Means

There are several benefits to an auxiliary means of heat removal on the secondary steam side. They are:

- (1) Necessity in emergency shutdown cases of removing decay heat from the core. This amounts to 5% of full power immediately after shutdown but rapidly decreases. Ideally this should not require any power source and should include a supply of cold water sufficient for several hours.
- (2) Maintaining the core power above say 20% full power would keep the slurry in suspension.
- (3) This would also reduce size of transient xenon peaks.
- (4) This would also reduce the requirements on the pressurizer or steam chest in as much as the complete change from 40% to 0% vapor would not be encountered save in extreme emergencies.

The conditions which might arise to cause fairly rapid changes in steam flow to the TG sets may include:

- (i) Loss of one TG set thru opening of generator breaker (or other causes) is a probable accident.
- (ii) Use of plant for short-term load regulation of - 10% of full load rating.
- (iii) Loss of cooling water is possible but not probable.
- (iv) Loss of TG sets is possible but not probable.
- (v) Loss of plant power possible.
- (vi) Start-up and shut-down.

These possibilities might be used as criteria for the size of the dumping condenser:

- (a) Effectiveness of a 25% of full load capacity dumping condenser.
 - (1) Effective for i, ii, vi.
 - (2) Possibly effective for iii, iv, v.
- (b) Effectiveness of a 100% of full load capacity
 - (1) Effective for i, ii, iii, vi.
 - (2) Possibly effective for iv, v.

Types of secondary steam dumping condensers:

- (1) High-pressure dumping transition on TG condenser.
- (2) Throttling into a heat exchanger which would deliver saturated steam at approximately atmospheric pressure to the TG condenser.
- (3) Throttling directly into TG condenser.
- (4) Separate and additional condenser which would condense at high pressure. This could possibly be built as a gravity system requiring no feed pump.

From the above criteria it is seen that the size of any condenser should be approximately 25% full load. The selection of the specific type is an economic one except that those which use the existing TG condensers (1,2,3) would not be effective if the original condition was the loss of that condenser through tube failure, CW loss, etc.

To summarize the case of secondary steam dumping it may be said that at least 6% of full load capacity is needed and that

the most probable economic choice would be 25% of full load capacity in a separate high-pressure condenser.

Pressure Relief System

Previous components (pressurizer, steam chest, dump condenser) are designed to keep pressure within design limits and if possible to keep core operating at 25% power to negate slurry settling problems.

Here the possibility of a rupture of the primary system with subsequent dispersal of radioactive products is considered. This accident is extremely remote but it seems that, if a pressurized system is designed without a means of pressure relief, it will always be theoretically possible either by deliberate or accidental methods. A pressure vessel of this size 10-20,000 ft³, and this design pressure, 1000-2000 psi, could create a serious explosion. The additional hazard of dispersing large quantities of radio-active products over a wide area makes it a dangerous accident.

The problem is not how to relieve the pressure but how to contain the dangerous radioactivity and the valuable fuel and D₂O.

Pressure relief valves and rupture discs are both approved means of limiting the pressure in the pressure vessel.

The relief valves could vent either from the steam chest or the bottom of the core. In the 1st. case they would vent principally from 1500 psi steam (saturated) with some radioactivity. In the second case they would vent liquid D₂O (which would flash to SH steam) plus fuel, thorium, and fission products.

	<u>Steam Vent</u>	<u>Slurry Vent</u>
D ₂ O conditions	SH steam	SH Steam
Radioactivity	Some	High conc.
Fuel	Slight	High conc.
ThO ₂	Slight	High conc.
Heat Removal cap.	-----same-----	
Heat removal from		
catch tank	No	Yes
Catch tank size	-----approximately same-----	

In either case a complete plant shutdown ensues. In the steam vent, only D₂O need be returned to the core however.

A suggested pressure relief system is shown in Figure 21.

FIGURE 21

The above size was arrived at by assuming that the core power immediately went to zero upon opening of the primary relief valve. This means that if the H_2O is originally at $80^{\circ}F$ and is kept below $200^{\circ}F$ and if the initial core pressure is 1500 psi and is cooled at $212^{\circ}F$ liquid, approximately 150,000 gallons of H_2O are required. Approximately 18,000 gallons of D_2O are condensed in the H_2O increasing its enrichment to 12%. The cost of recovering this D_2O is approximately \$75,000.

Some means of allowing for decay heat from the core must be provided. This would best be done in the secondary steam system.

Note that, although this calculation is optimistic since it neglects nuclear energy from the core (shutdown and decay), the size of the H_2O tank may be increased by a factor of 2 and the cost of D_2O recovery becomes approximately \$150,000 which is not a major catastrophe.

In addition, this is an emergency system and is not expected to operate more than once in 5 years as a pessimistic guess. Therefore it needs no design safety factor for, even in case of a rupture of the H_2O tank, the primary purpose of the system would have been served - prevention of a radioactive explosion. Note: If it is assumed that the nuclear power after rupture is approximately 1000 MW for 1 sec and that the space above the H_2O is equal to the H_2O volume the pressure in the H_2O tank would reach ~ 10 psi.

Vapor Fraction vs. Power Level

The above discussion has assumed a linear relation between vapor fraction and power level, "a" - Fig. 22. This would not be exactly true in any case and it should be theoretically possible to shape the curve in the manner desired over at least the range from 25% to 100% of full power.* It is probably safe to assume that this can be done since the mechanism of mass boiling is currently under investigation with some degree of success. A relation such as "b" in Figure 22 might be obtained.

FIGURE 22

It seems that, for most reactors, a characteristic such as "c" would lead to instability. A curve such as "b" between 25% and 100% of full power seems feasible with the proper design of baffles and steam separators.

* This could be done by a natural circulation boiler or combined natural rise and natural circulation.

The advantages of such a curve "b" are that the steam chest could be designed to handle only power changes between 25 and 100% of full power with smaller pressure changes and smaller steam chest volumes, and the core would be designed for smaller changes in liquid level during operation.

This point affects principally the numerical values used for the pressurizer and steam chest since they were based on 0 to 40% vapor range. The design features are unchanged however, the criteria becoming 25% to 100% power range and say 30% to 40% vapor fraction range.

5.3.2 Control of a Natural Circulation Boiling Reactor

The hydrodynamic analysis of a natural circulation reactor is presented in Appendix III, part 2 and assumes conservation of energy and conservation of mass of D_2O in the system and of fuel material in the liquid phase of the core.

For the specific design study reactor (500 M.W., etc.) a steam storage space volume ($16,000 \text{ ft}^3$) was determined by trial and error which yielded negative partial reactivity coefficients of power and pressure as illustrated by the resulting characteristic operating curves shown in Figures 23, 24, and 25.

Figure 24

²³³U-TH-D₂O REACTOR BOILING WITH NATURAL CIRCULATION

REACTIVITY vs POWER AT CONSTANT PRESSURES

Figure 25

U^{233} -TH-D₂O REACTOR BOILING WITH NATURAL CIRCULATION

REACTIVITY vs PRESSURE AT CONSTANT POWERS

U^{233} -TH-D₂O REACTOR WITH NATURAL CIRCULATION BOILING

OPERATING CURVE PRESSURE VS POWER

17.5 x 17.5 FT CORE

Fig. 23

5.4 Chemical Processing

5.4.1 General

One of the basic advantages of a homogeneous type reactor is that the fuel and fertile material may be continuously processed. This continuous operation maintains a high level of neutron economy (particularly important in a breeder) and minimizes the biological hazards associated with the operation of a pressurized radioactive system.

Two methods of processing a D_2O -mixed oxide slurry have been considered. One is a modification of the Thorex Process which has been proposed for the processing of a ThO_2 blanket. The other is a new proposal based upon the inherent properties of the slurry to be used and some assumptions as to the radiochemical behavior of the system.

In both processes no attempt is made to separate the thorium from the uranium or to obtain high decontamination factors. It would only be necessary to provide sufficient removal of neutron absorption cross-section to yield the economically optimum overall (reactor and process) neutron economy; biological tolerances are not necessary since the fuel in both processes can easily be regenerated.

5.4.2 Modified Thorex Process (Figure 26)

The main steps in processing a $D_2O - ThO_2 - UO_2$ slurry by the modified Thorex process and a description of the problems associated with each step are as follows:

1. Separation of D_2O from the solid particles with maximum recovery of D_2O .

This is accomplished by allowing the fission product heat to evaporate the D_2O . Calculations show that this is possible in a feasible time period.* By allowing the slurry to evaporate itself, there is no necessity of pumping concentrations greater than 1000 g/l of solids.

To obtain the removal of the last 5% moisture, a tray evaporator is used. This method has been proposed by the Vitro Corporation.⁽¹⁾

The hold-up time before D_2O evaporation and the required cooling period before processing are functions of the power level, uranium concentration, and process period. A plot of decay heat vs. cooling time for the uranium concentration of the specific design reactor is presented in Figure 28. (see Appendix IV for calculation method.)

2. Cooling solid residue sufficiently to prevent damage to the apparatus.

Calculations (see Appendix IV, part 2) indicate that there is sufficient time for addition of H_2O to the dry solids before the fission product heat raises the temperature of the evaporator beyond tolerable limits.

3. Dissolution of $ThO_2 - UO_2$

The $ThO_2 - UO_2$ is dissolved with HNO_3 and F^- ion catalyst under reflux conditions.

(1) KLX - 1611

* Calculations - Appendix IV

4. Separation of U and Th from Pa and fission products

A Thorex type solvent extraction flowsheet to separate Pa and fission products from the U and Th is used. The U resulting from Pa decay is recovered by reprocessing the material which has been stored through the solvent extraction system.

5. Regeneration of slurry

A number of methods have been used for the preparations of a mixed oxide of Th and U. (See Section 5.1.2)

One method which seems very applicable for integration with a Thorex processing system is the denitration of an aqueous solution of Th, U, and NO_3 . The aqueous stream from the second column of the system is evaporated to dryness, calcined at 500°C and slurried in D_2O .

The slurry is now ready for reactor operation.

5.4.3 "Thox" (Conceptual) Process

In this process the D_2O is evaporated, a fraction of the fission products is leached from the solid oxide with a selective solvent, and the slurry dried, calcined, and reslurried in D_2O . The feasibility of such a simple process is based upon the following argument.

The slurry particles to be used will be a micron or less in average size. Since the average path length of fission products is 6 microns, it is assumed that many fission fragments will escape from the particles and the newly formed Pa will not. Because of the extremely high surface activity of the ThO_2 , any particles which go out in the D_2O will probably be adsorbed on the ThO_2 surface.

If such a mechanism should indeed exist in the reactor, then the problem which must be solved is to find a reagent which will either dissolve or complex these fission particles away from the ThO_2 surface.

The specific fission products and the fraction of these which escape from the oxide particle and can be selectively removed from the solid oxide will determine the feasibility of such a proposal. At present, a few simple experiments are being carried on to determine if such a mechanism exists and can be capitalized on as mentioned.

If the above speculations should be correct, then the process would be as follows: (See Figure 27)

1. Slurry is continuously drawn from the reactor and stored until sufficient amounts have been accumulated.
2. D_2O is evaporated as proposed in the Thorex process above.
3. Solids are washed with a suitable leaching agent (e.g. HNO_3)
4. Solids are dried, perhaps in an evaporator using fission product heat since there will be no necessity to cool the slurry before processing as was the case in the solvent extraction systems.

5. D_2O added to solids and slurry stored for return to reactor.

The advantages of such a system are apparent.

1. No holdup for cooling of decay heat lowers inventory.
2. Simple equipment and few operations.
3. No dissolutions, or regeneration of slurry is necessary.

It is quite possible that such a system, although incapable of removing all the fissions product, may be easily integrated with a centralized Thorex plant, for complete fuel processing on a long time cycle.

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SCHEMATIC FLOW DIAGRAM
for
Thorex type process

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UNCLASSIFIED

SCHEMATIC FLOW DIAGRAM
for
Proposed Thox Process

FIG. 27
-89-

Fig. 28

40

-90-

60

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6.0 ECONOMICS OF POWER PRODUCTION

A critical economic analysis and optimization study could not be justified at this time since the technology of the boiling system is not sufficiently advanced. The preliminary groundwork has been laid for determining the fissionable and fertile material requirements as a function of core size, power, and breeding ratio, but until more is known about the power removal and control and fabricability aspects of such a system, little can be said about the final cost of such a plant.

6.1 Fissionable and Fertile Material Requirements

Critical concentrations of U^{233} and Th in a 1000 Mw $UO_2 \cdot ThO_2$ slurry (D_2O) system have been determined for various core sizes as a function of void fraction, (see Figures 29, 30, 31) with breeding ratio as the parameter. From these graphs one can immediately determine the critical mass, once the average void fraction is specified from the hydrodynamic correlations. The graphs cover the following ranges:

<u>Reactor Core Size</u> (nominal)		<u>Breeding</u>
<u>Diameter</u>	<u>Height</u>	<u>Ratio</u>
20'	x 20'	1.0
17.5'	x 17.5'	.9
15'	x 15'	

For an optimization study it is only necessary to extend this work to other power levels, breeding ratios, and core sizes.

Note: In all the nuclear calculations a poison fraction of .07 was used, principally because, with one set of costs and calculation ground rules, it was found by R. Briggs et al. (ORNL 1642) that such a poison fraction was approximately the economic optimum and that the analysis made to determine this could be applied as well to the boiling system under investigation.

6.2 Cost of Power for Specific Design

The cost of power was determined for the specific design system using the cost numbers and ground rules presented in Figure 32. The capital cost of the reactor plant (table 33) is extremely questionable, the power plant cost is good (see section 6.3), and the fuel costs are only as accurate as the nuclear and power removal calculations.

The fuel cost breakdown (see section 6.4) for two 500 Mw reactors, one 15' x 15' and the other 17.5' x 17.5' (nuclear core nominal dimensions), is illustrated in Figure 46 in the Appendix IV. It is interesting to note that the minimum cost in the 17.5' x 17.5' reactor occurs at a B.R.* of ~ 1.0 whereas in the 15' x 15' it occurs in the neighborhood of 0.9. For convenience the critical mass data for these two systems, in which the void fraction is specified, is plotted in Figure 34 as a function of breeding gain.

The cost summary is presented in Figure 35, the cost of power being 7.7 mls/kw hr.

* Breeding ratio

FIG. 29

FIG. 30

FIG. 31

FIGURE 32

BASIS FOR ECONOMIC EVALUATION

Value of materials:

U ²³³	\$25/gm
D ₂ O	\$40/lb
Th	\$10/lb

Investment assessments:

Depreciating capital	15%
Non-depreciating capital (U ²³³ , D ₂ O, etc.)	12%
Annual loss of D ₂ O	5%
Plant factor	0.80
Fuel process (Thorex) cost:	
Centralized plant	\$5.50/kg Th
Integrated plant	\$50/kg Th

COST DATA FOR REACTOR PLANT

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COST DATA FOR REACTOR PLANT (con't)

ITEM	DESCRIPTION	NO. REQUIRED	UNIT COST	TOTAL COST
<u>Low Pressure</u>				
L. P. Recombiner		1		2,000
Condenser	400 ft ² H ₂ O tubes D O & F.P. shell 2	1		20,000
Cold Trap & Charcoal Bed		1		2,000
Stillling Chamber	200 gal 15 psi	1		2,000
Dump Tank	5000 gal 5' I.D. x 35' long Agitated	11	20,000	220,000
D ₂ O Storage Tank	600 gal	1		4,000
Conc. Slurry Tank	600 gal Agitated	1		6,000
Piping				70,000
Instrumentation				40,000
				<hr/> 366,000

COST DATA FOR REACTOR PLANT (con't)

ITEM	DESCRIPTION	No. REQUIRED	UNIT COST	TOTAL COST
Auxiliary				
Relief Tank	250,000 gal, mild steel	1		50,000
Start up Boiler	Chemically fired, 2,500 KW, 200 psi steam	1		200,000
				<hr/> 250,000
Structure				
Reactor & Equipment cells				2,000,000
Crane				200,000
Control Room, Lab., & Process Area				250,000
Cell Ventilation				290,000
				<hr/> 2,740,000
Total Direct Cost			8,365,000	
Contractors overhead and fees (27%)			2,257,000	
Engineering and Inspection (10%)			<u>836,000</u>	
Total Plant Cost				11,460,000

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FIGURE 35

COST SUMMARY

Power Plant:

$$\frac{\$118.60^{(1)}}{\text{kw gross cap.}} \times \frac{32.4 \text{ kw gross}}{30.0 \text{ kw net}} \times 1.03^{(1)} \times \frac{0.15 \times 10^3 \text{ mills}}{365 \times 24 \times 0.8} = 2.82 \frac{\text{mills}}{\text{kwhr}}$$

Reactor plant: $\$12 \times 10^6$

1.7 "

Operation & Maintenance: Operation - 8% of fixed chg.

= .36 "

Maintenance - 10% of fixed chg.

= .45 "

Fuel cost (B.R. = 1.03)

	<u>Reactor</u>	<u>Process</u>		
U^{233}	.965	.172		
D_2O	.826	.010		
Th	<u>.050</u>	<u>.009</u>		
	1.831	.191	=	2.022
U^{233} makeup			=	.144
D_2O makeup			=	.346
Processing @ \$5.50/kg Th	(I)		=	.115
@ \$50/kg Th	(II)		=	1.04
				2.35 "
				3.29 "
			Total (I)	7.7 $\frac{\text{mills}}{\text{kwhr}}$
			Total (II)	8.6 "

(1) Unit cost and scale up factor from ORNL-1642

Figure 35 (cont.)

COSTS
(mills/kwhr)

Power plant			2.8
Reactor plant			1.7
Operation & Maintenance			.8
Fuel cost			2.4
Inventory	U ²³³	1.14	
	D ₂ O	.84	
	Th	.06	
Losses	U ²³³	-.14	
	D ₂ O	.35	
Processing	(@ \$5.50/kg Th)	.12	
	Total		<u>7.7</u>

6.3 TURBOGENERATOR PLANT ESTIMATED COST

Data from ORNL 1387 was used as a basis for estimating the dollars-per-kilowatt cost of the turbogenerator plant. The figures were based upon the cost of the Johnsonville steam plant of TVA and include all the steam-plant cost except for such parts as the boiler, coal and ash-handling equipment, etc., which can be replaced by a reactor. The following modification was made for inclusion in this report. The costs were multiplied by 1.078, a value obtained from the "Engineering News-Record", to reflect the changes in construction costs since 1951.

The modified cost figures include the cost of the entire power plant except the reactor, steam generating equipment, and the added facilities required for reactor operation which are included in the section "Cost of Reactor Plant". Turbogenerators are enclosed in the Johnsonville plant, and the cost is included in this data. If the turbogenerators are installed outdoors, there would be a savings of \$6.50 per kilowatt of capacity.

An estimate of the effect of plant capacity upon cost is shown in Figure 36.

POWER PLANT ESTIMATED COST

<u>Account No.</u>		<u>% of Total</u>		<u>\$/KW Capability</u>
310	Land and land rights	1.33	1.33	1.58
311	Structures and improvements	21.5	21.5	25.50
312	Feedwater equipment	2.89		
	Water supply and treatment	0.29		
	Boiler plant boards, instruments, and controls	1.91		
	Boiler plant piping	<u>6.36</u>		
	Total account 312		11.45	13.56
314	Turbogenerators	39.00		
	Circulating water system	6.62		
	Condenser	3.90		
	Miscellaneous turbogenerator equipment and piping	<u>0.65</u>		
	Total account 314		50.17	59.46
315	Accessory electrical equipment	6.10	6.10	7.24
316	Miscellaneous Power Plant equipment	1.55	1.55	1.85
342	Transmission Plant	7.90	7.90	9.38
343				
	Total	100.00	100.00	\$118.60

FIG. 36

6.4 FUEL COST

The fuel cost includes the following charges:

1. U^{233} , D_2O and Th investment in the reactor plant
2. U^{233} , D_2O , and Th investment in the process plant
3. U^{233} , and D_2O makeup (or credit) cost
4. Chemical processing cost

Items 1 and 3 are basically determined by the critical mass requirements as dictated by the reactor design, and the basic cost numbers and ground rules chosen to operate by; these items are essentially fixed.

Items 2 and 4 are somewhat more flexible in that there have been presented two alternate methods of processing, each one supposedly yielding different values of cost for these items.

Although the modified Thorex system presented in this study is basically simpler (since high D.F. is not required and U and Th need not be separated) than the process as described in ORNL CF-53-6-173 the costs presented in Figure 1 of that report for a small plant (100 kg Th/day) and the asymptotic value of the cost (> 3000 kg Th/day) were used to determine item 4 and a time of 60 days and 5 days were chosen as the U^{233} and D_2O holdup times, respectively, in such a plant, the U^{233} to allow for radioactive cooling and the D_2O as processing holdup time, to determine inventory charges.

The newly proposed (Thox) process, if feasible, would yield a considerable savings in both items 2 and 4, but it has neither been designed nor cost estimated, and consequently the cost in Appendix is only for the Thorex-type process.

7.0 APPENDICES

- I. Nuclear Calculations
- II. Power Removal
- III. Control
- IV. Chemical Processing

APPENDIX I

NUCLEAR CALCULATIONS

Part I. CALCULATION OF CRITICAL CONCENTRATIONS.

A. Critical Equation

The critical equation used is:

$$(1) \quad 1 = \eta f p \frac{1}{(1 + L^2 B^2)} \times \frac{1}{(1 + \tau B^2)}$$

where

- τ = Fermi Age
 L^2 = Diffusion Length
 B^2 = Buckling
 p = Resonance escape probability
 η = Number of neutrons generated per neutron absorbed in the uranium.,
 f = thermal utilization which equals the ratio of the absorptions in the uranium to the total absorptions.

Let:

$$y = \frac{\sum B}{\sum D_2 O}, \quad z = \frac{\sum 23}{\sum D_2 O}, \quad w = \frac{\sum O_2}{\sum D_2 O}, \quad \text{and} \quad u = \frac{\sum p}{\sum D_2 O}$$

where

- $\sum 23$ = Macroscopic absorption cross-section of Uranium²³³
 $\sum O_2$ = Macroscopic absorption cross-section of Thorium
 $\sum p$ = Macroscopic absorption cross-section of Poisons
 $\sum 13$ = Macroscopic absorption cross-section of Protoactinium
 $\sum D_2 O$ = Macroscopic absorption cross-section of D₂O

Let:

$$v = \frac{\Sigma p}{\Sigma 23} = \frac{u}{z}$$

$$x = \frac{\Sigma 13}{\Sigma 23} = \frac{y}{z}$$

L_0^2 = Diffusion length of pure D_2O

The following relationships hold:

$$(2) f = \frac{z}{1 + z + w + u + y}$$

$$(3) L^2 = \frac{L_0^2}{1 + z + w + u + y}$$

By substituting relationships (2) and (3) into Equation (1) and rearranging terms, (4) is obtained

$$(4) z = \frac{1 + w + L_0^2 B^2}{\frac{np}{1 + B^2 \tau} - (1 + v + x)}$$

The neutrons absorbed in Thorium per neutron absorbed in Uranium 233 is $\frac{w}{z}$.

The resonance capture in Thorium per neutron absorbed in Uranium 233 is

$$\frac{\gamma}{1 + B^2 \tau} (1 - p)$$

Not all of the protoactinium produced decays to uranium because of possible neutron capture by the protoactinium.

Define F as the amount of protoactinium which decays into uranium divided by the total amount of protoactinium present.

$$(5) F = \frac{\lambda_{13} + \lambda_{13c}}{\lambda_{13} + \lambda_{13c} + \sigma_{13a}}$$

λ_{13} = decay constant of ${}_{91}\text{Pa}^{233}$ in sec^{-1}

λ_{13c} = chemical processing constant

ϕ = neutron flux

σ_{a13} = microscopic absorption cross section of ${}_{91}\text{Pa}^{233}$.

The amount of ${}_{91}\text{Pa}^{233}$ produced is,

$$(6) \quad \frac{W}{Z} \frac{n_p}{1 + B^2 \tau} (1 - p).$$

The breeding ratio, which equals the atoms of uranium 233 produced per atom of Uranium 233 destroyed is equal to,

$$(7) \quad F = \left[\frac{W}{Z} \frac{n_p}{1 + B^2 \tau} (1 - p) \right]$$

Define ρ as the total absorption in Thorium divided by the thermal absorptions in Thorium

$$(8) \quad \rho = 1 \frac{\sum_{23}}{\sum_{-2}} \eta \frac{(1 - p)}{1 + B^2 \tau}$$

and

$$(9) \quad \frac{N_{13}}{N_{02}} = \frac{\rho \sigma_{a02} \phi}{\lambda_{13} c + \phi \sigma_{a13} + \lambda_{13}} = \frac{\text{atoms } {}_{91}\text{P}^{233}}{\text{atoms } {}_{90}\text{Th}^{232}}$$

The protoactinium poison fraction is,

$$(10) \quad \frac{\sum_{13}}{\sum_{02}} \times \frac{\frac{\sum_{a2}}{\sum_{D20}}}{\frac{\sum_{a3}}{\sum_{D20}}} = \frac{N_{13}}{N_{02}} \times \frac{\sigma_{a13}}{\sigma_{a02}} \times \frac{W}{Z} = X$$

In a boiling system whose average void fraction is f , the age and diffusion length are modified by the following:

$$\text{Fermi Age} = \frac{\tau}{(1 - f)^2}$$

$$(\text{Diffusion Length})^2 = \frac{L_0^2}{(1 - f)^2}$$

Finally, the flux is related to the power density by relationship (11),

$$(11) \quad P = \frac{\phi \sum_f}{3.38 \times 10^{10}}$$

where P is in $\frac{\text{kw}}{\text{liter}}$

B. Nuclear Data

Uranium 233

$$\eta = 2.32 \pm .03 \text{ temperature independent}$$

At 20°C

$$\sigma_a = 592 \pm 10 \text{ barns}$$

$$\sigma_f = 545 \text{ barns}$$

To find $\sigma_a(T)$ and $\sigma_f(T)$, where T is any temperature in degrees Kelvin, use the $\left(\frac{1}{v}\right)$ relation.

$$(1) \frac{\sigma_{20^\circ}}{\sigma(T)} = \left(\frac{T}{293^\circ}\right)^{.5}$$

At 285°C

$$\sigma_a = 426 \text{ barns}$$

$$\sigma_f = 393 \text{ barns}$$

Thorium

$$\text{At } 285^\circ\text{C}, \sigma_{a_{O_2}} = 5 \text{ barns}$$

D₂O

The following temperature relationships hold for the parameters of

D₂O:

$$(2) \rho(T) = \rho_{20^\circ\text{C}} \times \left(\frac{\rho_{20^\circ\text{C}}}{\rho(T)}\right)^2$$

where ρ is density at T degrees Kelvin

$$(3) L^2(T) = L^2(20^\circ\text{C}) \times \frac{\rho_{20^\circ\text{C}}}{\rho(T)} \times \frac{\sigma_s(20^\circ\text{C})}{\sigma_s(T)} \times \sqrt{\frac{T}{293^\circ\text{K}}}$$

where $\sigma_s(T)$ is scattering cross-section at T degrees Kelvin

$$(4) \sigma_a(T) = \sigma_a(20^\circ\text{C}) \times \frac{\rho(T)}{\rho_{20^\circ\text{C}}} \times \frac{L^2(20^\circ\text{C})}{L^2(T)} \times \frac{\rho_{20^\circ}}{\rho(T)}$$

At 20°C for heavy water whose composition is 99.75% D₂O and .25% H₂O,

$$\sigma_a = .00273 \text{ barns}$$

$$\rho = 1.103 \text{ gms/cm}^3$$

$$\tau = 120 \pm 6 \text{ cm}^2$$

$$L_o^2 = 1.35 \times 10^4 \text{ cm}^2$$

At 285°C

$$\rho = .861$$

$$\sigma_a = .00168 \text{ barns}$$

$$\tau = 197 \text{ cm}^2$$

$$L_o^2 = 3.1 \times 10^4 \text{ cm}^2$$

Resonance Escape Probability P

$$P = \text{Exp} \left\{ -\frac{N_{O2}}{\sum s} \times R_{\text{eff}} \right\}$$

$$R_{\text{eff}} = 8.33 \left[\frac{\sum s}{N_{O2}} \right]^{0.253}$$

The resonance escape probability is essentially due to the resonance capture by the Thorium. A plot of P versus concentration is given in Figure 37.

RESONANCE ESCAPE
PROBABILITY OF THORIUM

FIG. 37

C. Sample Calculation

Given: *Reactor whose height = 14.15 feet, radius = 7.68 feet

Temperature of Operation = 285°C

Power generated = 500 MW

Moderator is D₂O

Average void fraction = 23.3%

Volume = 7.48 X 10⁴ liters

Power density = 6.65 KW/liter

Find: The critical concentrations to give a breeding ratio of 1.05, assuming the protoactinium is not removed from the reactor.

Assume a Thorium concentration of 343 gms/liter D₂O. From Figure 37, at a corresponding 399 gm/kgm, p = .797

$$w = \frac{20}{232} \times \frac{.399 \text{ gms Th}}{\text{gm D}_2\text{O}} \times \frac{1}{.00168} = 102.5$$

$$\text{For the reactor in question, } B^2 = \left(\frac{\pi}{H} \right)^2 + \left(\frac{2.405}{R} \right)^2 = 2.25 \times 10^{-4} \text{ cm}^{-2}$$

$$1 + B^2(\text{Fermi Age}) = 1 + 2.25 \times 10^{-4} \times \frac{197}{(.767)^2} = 1.053$$

$$(\text{Diffusion Length})^2 B^2 = \frac{3.1 \times 10^4 \times 2.25 \times 10^{-4}}{(.767)^2} = 8.31$$

Assume v = .07

x = .0157

$$z = \frac{\frac{102.5}{2.32 \times .797} + 1 + 8.31}{1.053} = \frac{1 + .0157 + .07}{1.053} = 167.$$

*Dimensions are optimized for minimum leakage.

$$\frac{\text{gmsU}^{233}}{\text{liter D}_2\text{O}} = \frac{233}{20} \times 167 \times \frac{.00168}{426} \times \frac{86 \text{ kgms}}{\text{liter D}_2\text{O}}$$

$$= 6.58 \text{ gms/liter D}_2\text{O}$$

$$\sum f_{23} = 393 \times 10^{-24} \times \frac{.00658 \text{ gms}}{\text{cm}^3} \times (.767) \times \frac{6.023}{233} \times$$

$$10^{23} = 5.11 \times 10^{-3}$$

$$\phi = \frac{3.38 \times 10^{10}}{5.11 \times 10^{-3}} \times 6.65 \frac{\text{kw}}{\text{liter}} = .44 \times 10^{14}$$

$$\text{For } \lambda_{13c} = 0$$

$$\lambda_{13} = 2.93 \times 10^{-7}$$

$$F = \frac{1}{1 + 3 \times 10^{-16} \phi} = .987$$

$$\text{B. R.} = .987 \left[\frac{102.5}{167} + \frac{2.32}{1.053} \times (1 - .797) \right] = 1.05$$

$$\rho = 1 + \frac{167}{102.5} \times 2.32 \frac{(1 - .797)}{1.053} = 1.731$$

$$\frac{N_{13}}{N_{02}} = \frac{1.731 \times 5 \times .44 \times 10^{14} \times 10^{-24}}{100 \times 10^{-24} \times .44 \times 10^{14} + 2.93 \times 10^{-7}} = .00128$$

$$X = .00128 \times \frac{100}{5} \times \frac{102.5}{167} = .0157$$

which is value assumed.

Find the critical concentrations to give a breeding ratio of 1.05, assuming the protoactinium is removed at a rate

$$\lambda_{13c} = 7 \times 10^{-22} \phi \text{ sec}^{-1}$$

Try 331 gms/liter

$$p = .803 \quad w = 99$$

Assume $v = .07$

$x = .015$

$z = 156.8$

U^{233} concentration = 4.74 gms/liter D_2O

$\phi = .46 \times 10^{14}$

$$F = \frac{1 + 2.39 \times 10^{-15} \phi}{1 + 2.69 \times 10^{-15} \phi} = .988$$

B. R. = 1.05

$$\frac{\text{Here } N_{13}}{N_{02}} = \frac{\rho \phi \times 5 \times 10^{-24}}{8 \phi \times 10^{-22} + 2.93 \times 10^{-7}} \text{ giving } x = .0144$$

Part II. DENSITY DISTRIBUTION IN A BOILING REACTOR (See Figure 38)

The problem in question is to find the density distribution in a boiling cylindrical reactor such that it may be applied to such things as critical mass calculations.

The rate of steam production at a point in the reactor is proportional to the power generation.

For a given cylindrical reactor let:

W = lb. of mixture travelling past a point in the reactor per second - #/sec.

W_g = lbs. of vapor per lb. of mixture.

dt = time it takes the mixture to travel a distance dz .

A = cross sectional area through which mixture flows.

v = cubic feet per lb. of mixture.

h_{fg} = Btu's per lb. of vapor.

$G(z)$ = Btu's generated per cubic foot at point
"z" in the reactor in one second.

$N(z)$ = atoms per cubic foot at point "z" in the reactor.

K = conversion factor converting atoms per cubic foot into atoms per cm^3 .

$\phi(z)$ = neutron flux at point "z" in the reactor.

σ_f = microscopic fission cross section.

a = Btu's per fission.

$\rho(z)$ = density of mixture at point "z" in the reactor.

o - refers to bottom of reactor.

l - refers to top of reactor.

h = height of reactor.

v_g = cubic feet of vapor per pound of vapor.

v_f = cubic feet of fluid per pound of fluid.

$v_{fg} = v_g - v_f$ = volume change, or cubic feet change of volume per pound.

$$(1) \quad dz = \frac{W}{A} \frac{\# \text{ mixture}}{\text{ft}^2 \text{ sec}} \times v \frac{\text{ft}^3 \text{ mixture}}{\# \text{ mixture}} \times dt \text{ sec}$$

As the rate of formation of vapor depends on the power generation, then:

$$(2) \quad dw_g = \frac{G(z)}{h_{fg}} \frac{\text{Btu's}}{\frac{\text{Btu's}}{\# \text{ vapor}} \text{ sec ft}^3 \text{ mix.}} \times \frac{v \text{ ft}^3 \text{ mixture}}{\# \text{ mixture}} \times dt \text{ sec}$$

Dividing (2) by (1) the following is obtained:

$$(3) \quad \frac{d w_g}{d z} = \frac{G(z)}{h_{fg}} \frac{A}{W}$$

and

$$(4) \quad G(z) = K a N(z) \sigma_f \phi(z)$$

By the continuity principle

$$\frac{W \frac{\# \text{ mixture}}{\text{sec}} \times N_0 \frac{\text{atoms}}{\text{ft}^3 \text{ mixture}}}{\rho_0 \frac{\# \text{ mixture}}{\text{ft}^3 \text{ mixture}}} = \text{atoms/sec}$$

passing past any point in the reactor.

Multiplying the atoms/sec at point "z" by

$$\rho(z) \frac{\# \text{ mixture}}{\text{ft}^3} / W \frac{\# \text{ mixture}}{\text{sec}}, \text{ the following}$$

result is obtained:

$$(5) \quad N(z) = N_0 \frac{\rho(z)}{\rho_0}$$

By placing (4) and (5) into (3)

$$(6) \quad \frac{d_{wg}}{dz} = \frac{A K a \sigma_f}{W h_{fg}} \frac{N_0}{\rho_0} \rho(z) \phi(z)$$

$$(7) \quad \frac{1}{\rho(z)} \frac{\text{ft}^3 \text{ of mixture}}{\# \text{ of mixture}} = W_g \frac{\# \text{ Vapor}}{\# \text{ mixture}} \times v_g \frac{\text{ft}^3 \text{ vapor}}{\# \text{ vapor}}$$

$$+ (1 - W_g) \frac{\# \text{ fluid}}{\# \text{ mixture}} \times v_f \frac{\text{ft}^3 \text{ fluid}}{\# \text{ fluid}}$$

$$(8) \quad \frac{1}{\rho(z)} = v_f + v_{fg} W_g$$

Differentiating

$$(9) \quad v_{fg} \frac{d_{wg}}{dz} = 1/\rho^2(z) \frac{d\rho}{dz}$$

From (6) and (9)

$$(10) \quad \frac{-1}{\rho^3(z)} \frac{d\rho}{dz} = \left[\frac{K a A G_f N_0}{W h_{fg} \rho_0} v_{fg} \right] \phi(z) dz$$

$$\text{Let } C = \frac{K a A g_f N_o}{W h_{fg} \rho_o} V_{fg}$$

$$\text{Then (11)} \quad \frac{-1}{\rho^3(z)} d\rho(z) = C \phi(z) dz$$

Integrating (11)

$$\int_0^z -\frac{1}{\rho^3} d\rho = \int_0^z C \phi(z') dz'$$

$$(12) \quad \frac{1}{\rho^2(z)} - \frac{1}{\rho_o^2} = 2 \int_0^z C \phi(z') dz'$$

$$\text{As } \left[\frac{1}{\rho^2(z)} - \frac{1}{\rho_o^2} \right]_{z=h} = \frac{1}{\rho_1^2} - \frac{1}{\rho_o^2}$$

$$\text{then let } \int_0^h \phi(z) dz = 1$$

$$\text{and } 2C = \frac{1}{\rho_1^2} - \frac{1}{\rho_o^2}$$

Therefore (12) becomes

$$(13) \quad \frac{1}{\rho^2(z)} - \frac{1}{\rho_o^2} = \frac{1}{\rho_1^2} - \frac{1}{\rho_o^2} \int_0^z \phi(z') dz'$$

$$\text{or } (14) \quad \rho(z) = \rho_o \sqrt{\frac{1}{\left[\left(\frac{\rho_o}{\rho_1}\right)^2 - 1\right] \int_0^z \phi(z') dz' + 1}}$$

Part III. FLUX DISTRIBUTION OF A BOILING REACTOR (See Figure 38)

The model taken in this discussion is a right cylindrical reactor whose density distribution is uniform radially. This model is appropriate in that the results of this section can be applied in calculating the critical concentration and critical mass of a cylindrical reactor with a riser and a downcomer section.

As the density varies only axially, so do the diffusion coefficient and the macroscopic cross-sections. The diffusion equation may be written as:

$$(1) \nabla D(z) \nabla \phi(\rho, z) + \Sigma_a(z) \phi(\rho, z) = \nu \Sigma_f(z) \phi(\rho, z)$$

$$\phi(\rho, z) = \phi(z) R(\rho)$$

which leads to:

$$(2) 0 = \frac{1}{R(\rho)} \frac{1}{\rho} \frac{\partial}{\partial \rho} [\rho R'(\rho)] + \frac{1}{D(z) \phi(z)} \frac{\partial}{\partial z} [D(z) \phi'(z)]$$

$$+ \frac{\nu \Sigma_f(z)}{D(z)} - \Sigma_a(z)$$

$$\text{Let } (3) \frac{1}{R(\rho)} \frac{1}{\rho} \frac{d}{d\rho} [\rho R'(\rho)] = -K_1^2$$

where $K_1^2 = \frac{2.405^2}{R^2}$ which is obtained from the solution of (3) which is a Bessel's function and from the condition that the flux vanishes at the boundaries.

Also from (2) and (3)

$$(4) K_1^2 = \frac{1}{D(z) \phi(z)} \frac{d}{dz} [D(z) \phi'(z)] + \frac{\nu \Sigma_f(z) - \Sigma_a(z)}{D(z)}$$

$$\text{or } (5) 0 = \frac{d}{dz} [D(z) \phi'(z)] + \phi(z) [\nu \Sigma_f(z) - \Sigma_a(z) - K_1^2 D(z)]$$

The diffusion length and the cross sections are simply functions of the density.

$$(6) D(z) = D_0 \frac{\rho_0}{\rho(z)}$$

$$(7) \Sigma_a(z) = \Sigma_{a_0} \frac{\rho(z)}{\rho_0} \text{ where } D_0, \Sigma_{a_0} \text{ and } \Sigma_{f_0} \text{ are these parameters at the bottom of the reactor.}$$

$$(8) \Sigma_f(z) = \Sigma_{f_0} \frac{\rho(z)}{\rho_0}$$

By placing (6), (7) and (8) into (5) the following is obtained:

$$(9) \quad 0 = \frac{d^2 \phi(z)}{dz^2} - \frac{1}{\rho(z)} \frac{d \rho(z)}{dz} \times \frac{d \phi(z)}{dz} + \rho(z) \left[B_0^2 \left(\frac{\rho(z)}{\rho_0} \right)^2 - k_1^2 \right]$$

$$\text{where } B_0^2 = \frac{\nu \sum f_0 - \sum a_0}{D_0}$$

B_0^2 may be considered the buckling with respect to the material at the bottom of the reactor. The value of B_0^2 which satisfies the conditions that the flux vanish at the boundaries can give one the critical concentrations. That is:

$$B_0^2 D_0 + \sum a_0 = \nu \sum f_0$$

$$(10) \quad \frac{\nu \sum f_0}{\sum a_0} \times \frac{1}{1 + L_0^2 B_0^2} = 1$$

The critical concentrations can be obtained more accurately by using B_0^2 in a more sophisticated critical equation including the Fermi Age

$$(11) \quad \frac{\nu \sum f_0}{\sum a_0} \times \frac{1}{1 + L_0^2 B_0^2} \times \frac{1}{1 + B_0^2 \tau} \times P = 1$$

where P = resonance escape probability

τ = Fermi Age

Equation (9) is solved by difference equation methods. Let

$$\frac{d^2 \phi(z)}{dz^2} = \frac{y_{n+1} - 2y_n + y_{n-1}}{(\Delta z)^2}$$

$$\frac{d\phi(z)}{dz} = \frac{y_{n+1} - y_{n-1}}{2\Delta Z}$$

$$\frac{d\rho}{dz} = \rho'_n$$

Equation (9) in difference form is

$$(12) \quad \frac{y_{n+1} - 2y_n + y_{n-1}}{(\Delta Z)^2} - \left(\frac{\rho'_n}{\rho_n} \right) \left(\frac{y_{n+1} - y_{n-1}}{2\Delta Z} \right) + y_n \left[B_o^2 \left(\frac{\rho_n}{\rho_o} \right)^2 - K_1^2 \right] = 0$$

solving for y_{n+1}

$$(13) \quad y_{n+1} = \frac{-y_{n-1} \left[1 + \left(\frac{\rho'_n}{\rho_n} \right) \frac{\Delta Z}{2} \right] + y_n \left[2 - B_o^2 \left(\frac{\rho_n}{\rho_o} \right)^2 (\Delta Z)^2 + K_1^2 \Delta Z^2 \right]}{1 - \left(\frac{\rho'_n}{\rho_n} \right) \frac{\Delta Z}{2}}$$

The boundary conditions are

$y_0 = 0$ and $y_N = 0$ where 0 and N are at the extremities of the reactor. y_1 may be chosen to be any convenient number. However, the scale should be adjusted such that

$$\int_0^h \phi(z) dz = 1$$

or in terms of the parameters of the difference equation,

$$\sum_{n=0}^N y_n \Delta Z = 1$$

The flux and density distributions of the boiling reactor are determined as follows. First assume a density distribution and divide the

distribution into N intervals. Obtain $\frac{\rho'_n}{\rho_n}$ and $\left(\frac{\rho_n}{\rho_0}\right)^2$ for every interval such that use can be made in these in equation (13). Let $y_0 = 0$ and y_1 equal to a convenient number such as 1. Assume a value of B_0^2 and place it into equation (13). From equation (13) calculate y_2, y_3, y_N . If $y_N \neq 0$, then assume a different value of B_0^2 . Repeat this procedure until a value of B_0^2 is obtained which satisfies the condition that $y_N = 0$. Plot the flux distribution and normalize it such that $\int_0^h \phi(z) dz = 1$. Calculate a new density distribution from:

$$(14) \quad \rho(z) = \rho_0 \sqrt{\frac{1}{\left[\left(\frac{\rho_0}{\rho_1}\right)^2 - 1\right] \int_0^z \phi(z') dz' + 1}}$$

The density distribution, $\rho(z)$ obtained from (14) will probably differ from the assumed distribution. If this is so, use $\rho(z)$ obtained from equation (14) to find a new $\phi(z)$. Repeat this procedure until the density distribution used in equation (13) is identical with that calculated from equation (14). In actual calculations the density distribution converged rapidly toward its correct values. The value of B_0^2 which causes y_N to vanish for the correct density distribution is used in the critical calculations.

Part IV. EFFECT OF BOILING ON THE NUCLEAR CALCULATIONS

Single Region:

By the use of the method described, the flux and density distributions of various cylindrical reactors was determined: For a 19 foot high by 20.5 foot in diameter reactor, the flux and density distribution is shown in Figure 38.

FLUX AND DENSITY DISTRIBUTION OF A BOILING REACTOR

$$\frac{P_1}{P_0} = .5$$

$$B_0^2 = .198$$

$$\text{Size } H = 19'$$

$$R = 10.25'$$

$$\frac{\bar{P}}{P_0} = .642$$

FIG. 38

The B_0^2 used to make the flux vanish at the boundaries of the reactor for the above case is Q198.

The density of this reactor was chosen such that there are 50% voids at the top and pure D_2O on the bottom. By the use of the following critical equation;

$$(1) \frac{\nu \sum_{f0}}{\sum_{a0}} \frac{1}{1 + L_0^2 B_0^2} \times \frac{1}{1 + \tau B_0^2} p = 1$$

The critical concentration for a 1000 MW reactor required to give a breeding ratio of 1 are:

192 gms of thorium/liter D_2O

3.38 gms of U^{233} /liter D_2O

The average void fraction obtained from the density distribution curve in Figure 38 is 35.8%.

A reactor with a uniform density distribution requires a homogenized void fraction of 35.5% to give a breeding ratio of one for concentrations of Uranium and Thorium of 3.38 gms/liter D_2O and 192 gms/liters D_2O .

From this and several other calculations, it may be stated that the critical concentration may be obtained from a reactor whose voids are uniform throughout it provided that the density of this homogeneous reactor is equal to the average density of the boiling reactor.

Two Region: (Perturbation Approximation)

Suppose the reactor consisted of a downcomer and a riser portion. The downcomer would occupy about 40% of the cross sections of the reactor. See Figure 39 for a type of reactor which will be used as an example.

Fig.39

For an average void fraction of 35.5% in the riser, an average void fraction of 5.3% is obtained in the downcomer from Figure 40. The importance function, which indicates the effect of a region of the reactor in criticality calculations, is proportional to the flux squared. This means that changes in density in a region of low flux would have only a slight effect on the critical concentration. As the downcomer is located in a region of low flux, the critical concentration is essentially that of a reactor whose homogenized void fraction is 35.5%. This would give Uranium and Thorium concentrations of 3.38 gms/liter D_2O and 192 gms/liter D_2O .

From Figure 41, it can be seen that the exact critical concentrations are slightly higher than those given above because of the higher density region of the downcomer. Therefore, the critical concentrations are underestimated by a very small amount.

As there is a greater density of material in the downcomer, the critical mass is greater for this two region reactor.

FIG.40

FIG. 41

Using the same critical concentration for the two region reactor as was obtained for the one region reactor the critical mass is:

$$M_c = c \left[\left(\bar{\rho} / \rho_0 \right) A_1 + A_2 \left(\bar{\rho}_1 / \rho_0 \right) \right] \times V$$

A_1 = percentage of area which is riser

A_2 = percentage of area which is downcomer

$\bar{\rho}_1 / \rho_0$ = average density in the downcomer divided by the density of a non-void system

$\bar{\rho} / \rho_0$ = average density in the riser divided by the density of a non-void system

C = critical concentration in gms/liter D_2O

V = volume of reactor in liters

As an example:

c = 3.38 gms/liter D_2O of U^{233}

$\bar{\rho} / \rho_0$ = $1 - .355 = .645$

$\bar{\rho}_1 / \rho_0$ = $1 - .053 = .947$

A_1 = .6

A_2 = .4

$M_c = 3.38 \text{ gms} (.645 \times .6 + .947 \times .4) \times 17.7 \times 10^4 \text{ liters} = 458 \text{ kgm.}$

For a single region reactor of $\bar{\rho} / \rho_0 = .645$,

$M_c = 3.38 \text{ gms/liter of } D_2O \times 17.7 \times 10^4 \text{ liters} \times .645$
 $= 386 \text{ kgm.}$

Part V: TWO GROUP - TWO REGION CALCULATION

The boiling reactor has a riser and a downcomer region, each having a different average void fraction. A two group - two region calculation was performed to see if the results of the one group calculation were consistent. The diagram of the reactor is in Figure 42.

Figure 42

In both regions:

$$\begin{aligned}
 (1) \quad & -D_1 \nabla^2 \phi_1(\rho, z) + \sum_R \phi_1(\rho, z) = \nu \sum_f \phi_2(\rho, z) \\
 & -D_2 \nabla^2 \phi_2(\rho, z) + \sum_a \phi_2(\rho, z) = \rho_{th} \sum_R \phi_1(\rho, z)
 \end{aligned}$$

where 1 refers to the fast group and 2 refers to the slow group.

Let

$$\begin{aligned}
 \phi_1(\rho, z) &= Z(z) R_1(\rho) \\
 (2) \quad \phi_2(\rho, z) &= Z(z) R_2(\rho)
 \end{aligned}$$

where

$$\frac{\nabla_z^2 Z(z)}{Z(z)} = -\left(\frac{\pi}{2h}\right)^2$$

Substituting (2) into (1),

$$\begin{aligned}
 & -D_1 \nabla_\rho^2 R_1 + [\sum_R + D_1 \left(\frac{\pi}{2h}\right)^2] R_1 = \nu \sum_f R_2 \\
 (3) \quad & -D_2 \nabla_\rho^2 R_2 + [\sum_a + D_2 \left(\frac{\pi}{2h}\right)^2] R_2 = \rho \sum_R R_1
 \end{aligned}$$

Assume

$$\begin{aligned} R_1 &= A_1 Z(\rho) + C_1 W(\rho) \\ (4) \quad R_2 &= A_2 Z(\rho) + C_2 W(\rho) \end{aligned}$$

where

$$\begin{aligned} (5) \quad \nabla_\rho^2 Z(\rho) &= -l_1^2 Z(\rho) \\ \nabla_\rho^2 W(\rho) &= -l_3^2 W(\rho) \end{aligned}$$

Then

$$\begin{aligned} (6) \quad D_1 [A_1 l_1^2 Z + C_1 l_3^2 W] + [\Sigma_R + D_1 (\frac{\pi}{2h})^2] [A_1 Z + C_1 W] &= \nu \Sigma_f [A_2 Z + C_2 W] \\ D_2 [A_2 l_1^2 Z + C_2 l_3^2 W] + [\Sigma_a + D_2 (\frac{\pi}{2h})^2] [A_2 Z + C_2 W] &= \rho \Sigma_R [A_1 Z + C_1 W] \end{aligned}$$

By setting the coefficients of Z and W equal to zero,

$$\begin{aligned} (7) \quad A_2 \nu \Sigma_f &= A_1 \left\{ \Sigma_R + D_1 \left[l_1^2 + \left(\frac{\pi}{2h} \right)^2 \right] \right\} \\ C_2 \nu \Sigma_f &= C_1 \left\{ \Sigma_R + D_1 \left[l_3^2 + \left(\frac{\pi}{2h} \right)^2 \right] \right\} \\ A_1 \rho \Sigma_R &= A_2 \left\{ \Sigma_a + D_2 \left[l_1^2 + \left(\frac{\pi}{2h} \right)^2 \right] \right\} \\ C_1 \rho \Sigma_R &= C_2 \left\{ \Sigma_a + D_2 \left[l_3^2 + \left(\frac{\pi}{2h} \right)^2 \right] \right\} \end{aligned}$$

(8) Let

$$\xi = \left[l^2 + \left(\frac{\pi}{2h} \right)^2 \right]$$

From (7) the following quadratic equation is obtained in terms of ξ^2 whose solutions are μ^2 and $-\lambda^2$.

$$\begin{aligned} (9) \quad \rho \Sigma_R \nu \Sigma_f &= \left\{ \Sigma_R + D_1 \xi^2 \right\} \left\{ \Sigma_a + D_2 \xi^2 \right\} \\ \mu^2 &= -\frac{\chi_1^2 + \chi_2^2}{2} + \frac{1}{2} \left\{ (\chi_1^2 + \chi_2^2)^2 + 4 \chi_1^2 \chi_2^2 (k_\infty - 1) \right\}^{1/2} \end{aligned}$$

$$(10) \quad -\lambda^2 = -\frac{\chi_1^2 + \chi_2^2}{2} - \frac{1}{2} \left\{ (\chi_1^2 + \chi_2^2)^2 + 4 \chi_1^2 \chi_2^2 (k_\infty - 1) \right\}^{1/2}$$

where

$$\chi_1^2 = \frac{\Sigma_R}{D_1}, \quad \chi_2^2 = \frac{\Sigma_a}{D_2}, \quad k_\infty = \frac{\nu \Sigma_f \rho}{\Sigma_a} = \eta f \rho$$

From (8)

$$\begin{aligned} (11) \quad \mu^2 &= l_1^2 + \left(\frac{\pi}{2h} \right)^2 \\ -\lambda^2 &= l_3^2 + \left(\frac{\pi}{2h} \right)^2 \end{aligned}$$

or $l_1^2 = \mu^2 - \left(\frac{\pi}{2h}\right)^2$

$$l_3^2 = -\left[\lambda^2 + \left(\frac{\pi}{2h}\right)^2\right]$$

From (7) and (8)

$$(12) \quad A_2 = \frac{\Sigma_R + D_1 \mu^2}{\Sigma_f} A_1 = \frac{p \Sigma_R}{\Sigma_a + D_2 \mu^2} A_1$$

$$C_2 = \frac{\Sigma_R - D_1 \lambda^2}{\Sigma_f} C_1 = \frac{p \Sigma_R}{\Sigma_a - D_2 \lambda^2} C_1$$

or

$$A_2 = S A_1 \quad \text{and} \quad C_2 = T C_1$$

Now

$$(13) \quad R_1 = A_1 Z(\rho) + C_1 W(\rho)$$

$$R_2 = S A_1 Z(\rho) + T C_1 W(\rho)$$

where
$$S = \frac{\Sigma_R + D_1 \mu^2}{\Sigma_f} = \frac{p \Sigma_R}{\Sigma_a + D_2 \mu^2}, \quad T = \frac{\Sigma_R - D_1 \lambda^2}{\Sigma_f} = \frac{p \Sigma_R}{\Sigma_a - D_2 \lambda^2}$$

For region R (the riser)

$$R_1^R = A_1^R Z^R(\rho) + C_1^R W^R(\rho)$$

$$(14) \quad R_2^R = S A_1^R Z^R(\rho) + T C_1^R W^R(\rho)$$

where

$$Z^R(\rho) = J_0(l_1^R \rho)$$

$$W^R(\rho) = I_0(l_3^R \rho)$$

Therefore,

$$(15) \quad R_1^R = A_1^R J_0(l_1^R \rho) + C_1^R I_0(l_3^R \rho)$$

$$R_2^R = S A_1^R J_0(l_1^R \rho) + T C_1^R I_0(l_3^R \rho)$$

For region D (the downcomer)

$$(16) \quad R_1^D = A_1^D Z^D(\rho) + C_1^D W^D(\rho)$$

$$R_2^D = A_1^D Z^D(\rho) + C_1^D W^D(\rho)$$

$$(17) \quad Z^D(\rho) = J_0(l_1^D \rho) + \Gamma Y_0(l_1^D \rho)$$

$$W^D(\rho) = I_0(l_3^D \rho) + \delta K_0(l_3^D \rho)$$

Using the condition that the flux vanishes at the boundary,

$$(18) \quad Z^D(b) = 0 = J_0(l_1^D b) + \Gamma Y_0(l_1^D b)$$

$$\Gamma = - \frac{J_0(l_1^D b)}{Y_0(l_1^D b)}$$

$$(19) \quad W^D(b) = 0 = I_0(l_3^D b) + \delta K_0(l_3^D b)$$

$$\delta = - \frac{I_0(l_3^D b)}{K_0(l_3^D b)}$$

$$(20) \quad Z^D(\rho) = J_0(l_1^D \rho) - \frac{J_0(l_1^D b)}{Y_0(l_1^D b)} Y_0(l_1^D \rho)$$

$$W^D(\rho) = I_0(l_3^D \rho) - \frac{I_0(l_3^D b)}{K_0(l_3^D b)} K_0(l_3^D \rho)$$

The boundary condition of the continuity of the current and the

flux at the downcomer-riser interface give:

$$R_1^R(a) = R_1^D(a)$$

$$R_2^R(a) = R_2^D(a)$$

$$(21) \quad D_1^R \frac{dR_1^R}{d\rho} \Big|_{\rho=a} = D_1^D \frac{dR_1^D}{d\rho} \Big|_{\rho=a}$$

$$D_2^R \frac{dR_2^R}{d\rho} \Big|_{\rho=a} = D_2^D \frac{dR_2^D}{d\rho} \Big|_{\rho=a}$$

Condition (21) gives rise to a fourth order homogeneous equation.

The relationship among the various quantities obtained by setting the fourth order determinant equal to zero is:

$$(22) \quad \frac{Z_R'}{Z_R} = \frac{fh - g\ell}{D_1^R f - S D_2^R g}$$

$$\text{where } f = \left[\left(\Delta D_2^D \frac{Z_0'}{Z_0} - t D_2^D \frac{W_0'}{W_0} \right) \left(\frac{T-A}{A-t} \right) - \left(T D_2^R \frac{W_R'}{W_R} - \Delta D_2^D \frac{Z_D'}{Z_D} \right) \right]$$

$$g = \left[\left(D_1^D \frac{Z_0'}{Z_0} - D_1^D \frac{W_0'}{W_0} \right) \left(\frac{T-A}{A-t} \right) - \left(D_1^R \frac{W_R'}{W_R} - D_1^D \frac{Z_D'}{Z_D} \right) \right]$$

$$h = \left[D_1^R \frac{W_R'}{W_R} + \left(D_1^D \frac{Z_0'}{Z_0} - D_1^D \frac{W_0'}{W_0} \right) \left(\frac{S-T}{A-t} \right) \right]$$

$$\ell = \left[T D_2^R \frac{W_R'}{W_R} + \left(\Delta D_2^D \frac{Z_0'}{Z_0} - t D_2^D \frac{W_0'}{W_0} \right) \left(\frac{S-T}{A-t} \right) \right]$$

All of the above quantities are at $r = a$.

$$\text{For the riser, } Z^R = J_0(l_1^R a)$$

$$\frac{d}{d\rho} (Z^R) \Big|_{\rho=a} = Z_R' = -l_1^R J_1(l_1^R a)$$

$$(23) \quad W^R = I_0(l_3^R a)$$

$$\frac{d}{d\rho} (W^R) \Big|_{\rho=a} = W_R' = l_3^R I_1(l_3^R a)$$

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For the downcomer

$$\begin{aligned}
 Z^0 &= J_0(l_1^0 a) - \frac{J_0(l_1^0 b)}{Y_0(l_1^0 b)} Y_0(l_1^0 a) \\
 (24) \quad \frac{d}{d\rho}(Z^0) \Big|_{\rho=a} &= Z_0' = -l_1^0 J_1(l_1^0 a) + l_1^0 \frac{J_0(l_1^0 b)}{Y_0(l_1^0 b)} Y_1(l_1^0 a) \\
 W^0 &= I_0(l_3^0 a) - \frac{I_0(l_3^0 b)}{K_0(l_3^0 b)} K_0(l_3^0 a) \\
 \frac{d}{d\rho}(W^0) \Big|_{\rho=a} &= W_0' = l_3^0 I_1(l_3^0 a) + l_3^0 \frac{I_0(l_3^0 b)}{K_0(l_3^0 b)} K_1(l_3^0 a)
 \end{aligned}$$

The constants used in the two group calculation are as follows:

$$(25) \quad \Sigma_R = \frac{D_1}{\tau}$$

Let the zero subscript (or superscript) refer to the constant with no voids and "f" equal to the void fraction.

$$\begin{aligned}
 (26) \quad D_1 &= \frac{D_{10}}{(1-f)} \\
 D_2 &= \frac{D_{20}}{(1-f)}
 \end{aligned}$$

*where

$$D_{10} = 1.464 \quad (\text{at } 285^\circ\text{C})$$

$$D_{20} = 1.215$$

Also

$$(27) \quad \Sigma_a^0 = \Sigma_{a_{23}}^0 + \Sigma_{a_{02}}^0 + \Sigma_{a_{03}}^0 + \Sigma_p^0 + \Sigma_{a_{13}}^0$$

$$(28) \quad \Sigma_a = \Sigma_a^0 (1-f)$$

The values of τ and the other constants are indicated in the one group calculation section.

The radial flux plot obtained from the two group method is shown in Figure 42.

Fig. 42

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Comparison of Results

REACTOR SIZE	TH. CONC. GMS./LITER D ₂ O	CRITICAL CONCENTRATION GMS. 23/liter D ₂ O	
		ONE GROUP	TWO GROUP TWO REGION
15'H. x 15' dia.	700	22.4	24.5
17½' x 17½'	400	7.60	7.82
17½' x 17½'	250	4.23	4.47

Part VI: SLURRY SETTLING - SAFETY PROBLEM

Given: A cylindrical homogeneous breeding reactor whose properties are as follows:

Height 16.5 feet

Radius 8.94 feet

Volume 1.18×10^5 liters

Operating Temperature 285°C

With no poisoning present, the critical concentrations are:

107.5 gms/liter of Thorium

1.35 gms./liter of Uranium

Void fraction = 0

Problem: If the slurry settles out in the form of a 16 foot (244 cm radius) diameter sphere, find whether the assembly is subcritical or supercritical.

Concentration of Uranium in the sphere = $\frac{1.18 \times 1.35}{.605} = 2.63$ gms/liter where $.605 \times 10^5$ liters is the volume of the sphere.

Concentration of Thorium = $\frac{1.18}{.605} \times 107.5 = 210$ gms/liter

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For nuclear calculations this assembly is assumed to have an infinite D₂O reflector. The critical equation for an infinitely reflected spherical reactor is:

$$(1) \quad BR \cot BR - 1 = - \frac{D_r}{D_c} (\kappa R + 1)$$

$$\text{where } B^2 = \frac{\eta f p - 1}{M^2} \quad \text{and} \quad \kappa^2 = \frac{\Sigma_a D_{20}}{D_{20}}$$

$$f = \frac{z}{z + w + 1}, \quad z = \frac{\Sigma_{a23}}{\Sigma_a D_{20}}, \quad w = \frac{\Sigma_{a02}}{\Sigma_a D_{20}}$$

If, for a Uranium concentration of 2.63 gms/liter and a Thorium concentration of 210 gms/liter, the critical radius to satisfy (1) is greater than 244cm. then the 16 foot diameter spherical assembly is subcritical.

$$\kappa^2 = \frac{\Sigma_{aD_{20}}}{D_{D_{20}}} = \frac{4.35 \times 10^{-5}}{1.215} = .6 \times 10^{-4}$$

$$\kappa = .00775$$

$$B^2 = \frac{2.32 \times .512 \times .865 - 1}{435} = .345 \times 10^{-4}$$

$$B = .00587$$

Equation (1) becomes:

$$(.00587) R \cot (.00587) R = -1.085 (.006) R - .085$$

for which

$$300 < R_{\text{critical}} < 400 \text{ cm.}$$

Therefore the 244 cm. radius assembly is subcritical.

For smaller spheres formed from the same slurry, the resulting assembly is also subcritical as resonance absorption increases out of proportion to the other factors involved in neutron economy. That is $f = \frac{z}{z + w + 1}$

approaches a constant value as the sphere shrinks (z gets larger, w gets larger, and w/z remains constant). However, p will get smaller as the sphere shrinks. Therefore, $Bm^2 = \frac{\eta f p}{M^2} - 1$ will decrease for higher Thorium and Uranium concentrations and these assemblies will always remain subcritical ($1 > k_{eff}$).

APPENDIX II
POWER REMOVAL

CORE HYDRODYNAMICS ANALYSIS

Analysis:

In the majority of reactor studies completed as of the present date, the relationships between engineering and nuclear aspects (i. e., heat generation from neutron flux and fission potentiality) have been easily derived. However, in the present case, because of the stipulations of boiling fuel medium and circulation of that medium as a result of a density difference between riser and downcomer regions, the relationships are no longer so simple. Therefore, the following section attempts to develop the needed equations.

If one considers the model (Page 140), he will notice that the fuel liquid mixture passing station "f" into the downcomer, of cross-sectional Area A_D , consists of fuel and liquid not vaporized in the previous cycle and condensed D_2O from the heat exchangers. The fuel-liquid mixture at saturation conditions at "f", with no entrainment of vapor, then passes down through the downcomer, of height z_o , some of the liquid vaporizing, as the mixture moves, due to heat generation. At station "B", the fuel-liquid-vapor mixture enters the riser, of area A_R , height z_R , and passes up, more liquid vaporizing as it moves. At station "2", it enters the separator region, where the vapor goes to the heat exchanger, and the fuel and remaining liquid return to the downcomer to repeat the cycle. Since flow continuity is insisted on throughout the core, the density, designated by ρ , velocity by U , and area by A , all with appropriate sub-letters, must vary accordingly. For all practical purposes, the fluid-vapor mixture is considered incompressible.

AVERAGE CORE DENSITY IS $\bar{\rho}$

HYDRODYNAMIC CORE MODEL

Fig. 43

Therefore, by continuity,

$$\rho_i A_D U_f = \rho_2 A_R U_2 \quad (1)$$

Also the change in the volume flow rate between "2" and "f" is equal to the volume of vapor given off, which in turn is proportional to the average power density in the reactor.

$$\text{So that } U_f A_D - U_2 A_R + \frac{P v_{fg}}{h_{fg}} [A_D Z_D + A_R Z_R] = 0 \quad (2)$$

where P = average power density, BTU/sec.-ft³

v_{fg} = change in specific volume, liquid to vapor, ft³/lb.

h_{fg} = heat of vaporization, BTU/lb.

$$\text{Let } \frac{A_R}{A_R + A_D} = \beta, \quad \frac{Z_D}{Z_R} = H \quad (3)$$

Substituting (3) into (2), and (1) into (2) (for $U_f A_D$), and solving for P ,

$$P = \frac{U_2}{Z_R} \left(\frac{1}{\frac{1-\beta}{\beta} H + 1} \right) \left(\frac{h_{fg}}{v_{fg}} \right) \left(1 - \frac{\rho_2}{\rho_i} \right) \quad (4)$$

If the driving pressure in the core causing circulation is equal to $\rho_o Z_D - \rho_i Z_R$ (assuming that all energy picked up in the fall to Z_D , should Z_D be less than Z_R , is lost in turbulence), then the resisting pressure must be the pressure drops due mainly to acceleration and fitting friction losses. The latter may be represented by $\bar{\rho} K \frac{U_2^2}{2g}$, where K is to be defined.

$$\text{Therefore } Z_R (\rho_o H - \rho_i) = \bar{\rho} K \frac{U_2^2}{2g} \quad (5)$$

where ρ_i , ρ_o , and $\bar{\rho}$ are average riser, downcomer, and core densities,

respectively.

Substituting for Z_D and Z_R from (3) into (5) and solving for U_2 .

$$U_2 = \left\{ \frac{2g Z_R}{K} \left[\frac{\rho_o H - \rho_i}{\bar{\rho}} \right] \right\}^{1/2} \quad (6)$$

Substituting (6) into (4), and rearranging

$$\frac{P_{v_{fg}}}{h_{fg}} \left(\frac{K Z_R}{2g} \right)^{1/2} = \left(\frac{1}{\frac{1-\theta}{\theta} H + 1} \right) \left(1 - \frac{\rho_2}{\rho_1} \right) \left[\frac{\rho_o H - \rho_i}{\bar{\rho}} \right]^{1/2} \quad (7)$$

(note: Eqns. 1 through 7 are Zmola's eqns. from RST-13 adapted to meet the present exigencies.)

Now a relation must be found between densities.

It is convenient to consider the formation of vapor in the system as being that of the formation of a single bubble which steadily grows in size as it goes through downcomer and riser. At any one point, the fractional change in bubble volume is proportional to the heat generation per unit vapor volume at that point.

$$\begin{aligned} \text{That is, } & \frac{G \left(\frac{\text{BTU}}{\text{sec} - \text{ft}^3 \text{ mixture}} \right) \times v_g \frac{\text{ft}^3 \text{ steam}}{\text{lb. steam}} \times \Delta T (\text{sec})}{\rho \left(\frac{\text{lb. mixture}}{\text{ft}^3 \text{ mixture}} \right) \times w_g \left(\frac{\text{lb. steam}}{\text{lb. mixture}} \right) \times h_{fg} \left(\frac{\text{BTU}}{\text{lb steam}} \right) \times v_g \left(\frac{\text{ft}^3 \text{ steam}}{\text{lb. steam}} \right)} \\ & = \frac{\Delta V (\text{ft}^3 \text{ steam change})}{V (\text{ft}^3 \text{ steam present})} \end{aligned}$$

where the terms are as defined.

$$\text{Therefore, } \frac{G \Delta t}{\rho w_g h_{fg}} = \frac{\Delta V}{V} \quad (8)$$

But

$$\frac{\Delta V}{V} = \frac{\Delta w_g v_g}{w_g v_g} \quad (\text{since mixture weights cancel out}) \quad (9)$$

Substituting (9) into (8) and rearranging:

$$\frac{\Delta w_g}{\Delta t} = \frac{1}{h_{fg}} \frac{G}{\rho} \quad (10)$$

But G and ρ are dependent on t insofar as fluid and vapor velocities are concerned. Because of buoyant forces acting on the vapor, there will be slip between vapor and fluid, which may or may not be negligible. At any rate, a vector diagram of the mixture velocities shows that, in the

$$\text{Downcomer: } U_g = U_f - U_s \quad (11)$$

$$\text{Riser: } U_g = U_f + U_s \quad (12)$$

where U_g = vapor velocity, fps

U_f = fluid velocity, fps

U_s = slip velocity, fps

Considering first the riser:

$$U_g = \frac{\Delta Z_g}{\Delta t} = U_f + U_s \quad (13)$$

Substituting ΔZ_g for Δt , (13) into (10)

$$\frac{\Delta W_g}{\Delta Z_g} = \frac{1}{h_{fg}} \frac{G}{\rho(U_f + U_s)} \quad (14)$$

If one considers the flow of vapor as being practically a continuous stream, then conservation of mass may be applied as follows:

$$W = \rho_f A_f U_f + \rho_g A_g U_g \quad (15)$$

where W = liquid-vapor flow rate, lb/sec = constant

$$\text{Also } A_f + A_g = A_R \quad (16)$$

where A_f = cross-sectional flow area of fluid

A_g = cross-sectional flow area of vapor

Substituting (16) and (13) into (15) and solving for U_f ,

$$U_f = \frac{W - \rho_g A_g U_s}{\rho_f (A_R - A_g) + \rho_g A_g} \quad (17)$$

Substituting (17) into (14):

$$\frac{\Delta W_g}{\Delta Z_g} = \frac{G [\rho_f (A_R - A_g) + \rho_g A_g]}{\rho h_{fg} [U_s \rho_f (A_R - A_g) + W]} \quad (18)$$

Some definition of A_g is necessary. In order that the derivation not be too involved, it will be assumed momentarily that figuratively the vapor in a section in the riser or downcomer passage may be lumped together into the shape of a right circular cylinder. Actually, this is somewhat absurd; however, it serves to present the basic variability of the factors involved.

Therefore, in the riser, the picture at, say, position Z would be as shown:

where in volume $V_f + V_g$ there is a vapor bubble of cross-sectional area A_g and height h , and an annulus of area A_f and height h .

$$\text{The bubble mass} = \rho_g h A_g \quad (19)$$

$$\text{Fluid mass} = \rho_f h A_f = \rho_f h (A_R - A_g) \quad (20)$$

$$\text{Fuel mass} = M$$

Concerning the fuel mass, from continuity,

$$\frac{W \rho_{Ff}}{\rho_f} = \text{lb. of fuel/sec. introduced, a constant}$$

where:

$$\rho_F = \text{fuel concentration, lb./ft}^3\text{-mixture}$$

$$W = \text{liquid-vapor flow rate, lb./sec.}$$

ρ = liquid-vapor density, lb/ft³-mixture

$$\therefore \frac{w \rho_{Ff}}{\rho_f} = \frac{w \rho_F}{\rho}$$

$$\rho_F = \frac{\rho_{Ff}}{\rho_f} \rho \quad (21)$$

Therefore the liquid-vapor fuel density = $\rho + \frac{\rho_{Ff}}{\rho_f} \rho$

$$= \left(1 + \frac{\rho_{Ff}}{\rho_f}\right) \rho \quad (22)$$

So that $M = \rho_F A_R h$

$$= \frac{\rho_{Ff}}{\rho_f} \rho A_R h \quad (23)$$

Therefore, the weight fraction of vapor at Z is equal to

$$w_g = \frac{\rho_g A_g}{\rho_f (A_R - A_g) + \rho_g A_g + \frac{\rho_{Ff}}{\rho_f} \rho A_R} \quad (24a)$$

In order that an answer might be obtained as soon as possible, when these equations were developed, it was decided to ignore fuel weight; as a result, the following work, to a certain point would hold only for negligible fuel weight. (But if slip velocity is ignored, then fuel weight does not enter into the calculations anyway, because it can then have no differential effect on liquid and vapor velocity).

Finally, solving for A_g ,

$$A_g = \frac{w_g A_R \rho_f}{w_g (\rho_f - \rho_g) + \rho_g} \quad (24b)$$

Substituting (24b) into (18), and simplifying,

$$\frac{\Delta w_g}{\Delta z_g} = \frac{G}{\rho h_{fg} \left[U_s (1 - w_g) \frac{W}{A_R \rho} \right]} \quad (25)$$

The mixture specific volume at z is equal to

$$\frac{1}{\rho} = v_f + w_g v_{fg} \quad (26a)$$

where v_f = fluid specific volume, $\text{ft}^3/\text{lb.}$

$v_{fg} = v_g - v_f$ (v_g is vapor specific volume)

The derivative with respect to ρ is

$$d\left(\frac{1}{\rho}\right) = -\frac{1}{\rho^2} d\rho = v_{fg} dw_g \quad (26b)$$

(Were fuel weight considered,

$$\frac{1}{\rho} = v_f + w_g v_{fg} + w_F (v_F - v_f)$$

where w_F = fuel weight fraction of mixture = $\frac{\rho_{Ff}}{\rho_f + \rho_{Ff}}$

v_F = fuel specific volume

Since w_F and v_F are constant, the derivative is the same as above.)

Substituting (23) into (22), and changing increments to differentials:

$$\frac{dp}{dz} = \frac{-G\rho v_{fg}}{h_{fg} \left[\frac{U_s v_g}{v_{fg}} + \frac{1}{\rho} \left(\frac{W}{A_R} - \frac{U_s}{v_{fg}} \right) \right]} \quad (27)$$

$$G, \text{ being the heat generation} = K''' \Sigma_f \Phi = K''' \sigma_f N_F \Phi \quad (28)$$

where K = conversion factor

Σ_f = macroscopic fission cross-section, cm^{-1}

σ_f = microscopic fission cross-section, cm^2

N_F = fuel concentration, atoms/cm^3

Φ = flux, $\text{neutrons}/\text{cm}^2 - \text{sec}$

But, from 22, since $N_F = C\rho_F$

where C is a constant,

$$N_F = \frac{N_{Ff}}{\rho_f} \rho \quad (29)$$

Substituting (29) into (28),

$$G = K'' \rho \Phi \quad (30)$$

Now Φ is axially and radially dependent upon position. If it is assumed that the density in the radial direction, is roughly uniform, although of different magnitude in riser as opposed to downcomer, then the further assumption may be made that, radially, the heat generation varies as the flux which in turn varies as a Bessel function. For simplicity, then, the flux may be averaged over the cross-section.

$$\therefore \Phi = \Phi(r, z) = \int_0^{\sqrt{\beta}} 2\pi r J_0(2.405 r) \Phi(z) dr$$

$$\begin{aligned} \text{where } \beta &= \frac{\text{Cross-sectional area of riser}}{\text{Total cross-sectional area}} \\ &= \frac{A_R}{A_R + A_D} \end{aligned}$$

$$\therefore \Phi = \frac{2\pi\sqrt{\beta}}{2.405} J_1(2.405\sqrt{\beta}) \Phi(z) \quad (31)$$

Substituting (31) into (30):

$$G = K' \sqrt{\beta} J_1(2.405\sqrt{\beta}) \rho \Phi(z) \quad (32)$$

and (32) into (27):

$$\frac{d\rho}{dz} = \frac{-K' \sqrt{\beta} J_1(2.405\sqrt{\beta}) \rho^3 \Phi(z)}{\frac{U_s v_g}{v_{fg}} \rho + \frac{W}{A_R} - \frac{U_s}{v_{fg}}} \quad (33)$$

With respect to the downcomer, after a similar analysis,

$$\frac{d\rho}{dz} = \frac{-K [J_1(2.405) - \sqrt{e} J_1(2.405 \sqrt{e})] \rho^3 \phi(z)}{-\frac{U_s v_g}{v_{fg}} \rho + \frac{W}{A_d} + \frac{U_s}{v_{fg}}} \quad (34)$$

Solution of the two equations may now be made providing $\phi(z)$ is known, and U_s is considered constant. By a further refinement, however, U_s may be taken as variable.

Consider a bubble in the riser: Independent of the mixture movement, a force balance on that bubble shows buoyant forces equal to drag:

1.

$$\rho_g V_g = f_D \rho_f \frac{U_s^2 A_g}{2g}$$

where V_g = bubble volume, ft^3

f_D = drag coefficient

A_g = some characteristic area, ft^2

$$\text{Rearranging, } U_s = \left[\frac{2g (\rho_f - \rho_g) V_g}{\rho_f f_D A_g} \right]^{\frac{1}{2}}$$

(Again, if fuel weight were taken into account, since the fuel would mostly remain in the liquid, then ρ_f above would become $\rho_f + \frac{\rho_{Ff}}{1 - w_g - w_F}$).

If the assumption of cylindrical bubbles is continued, then one might expect such a bubble to possess minimum surface area for a cylinder with fixed volume.

$$\text{For such a cylinder } h = \left(\frac{A_g}{\pi} \right)^{\frac{1}{2}} \quad (36)$$

Also, from aerodynamic theory, for a cylinder, in connection with f_D , the characteristic area used is $A_g \cdot 2$. (37)

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1. Zmola, P. C., RST -13, August '54
 2. Binder, R. C., pg. 172, Fluid Mechanics

Substituting (24b), (35), and (36) into (34),

$$U_s = \left\{ \frac{2g(\rho_t - \rho_g) \left[\frac{w_g A_R \rho_t}{\pi w_g(\rho_t - \rho_g) + \pi \rho_g} \right]^{1/2}}{\rho_t f_D} \right\}^{1/2} \quad (38)$$

Substituting (26a), and (38) into (34) and simplifying:

$$\frac{d\rho}{dz} = \frac{-K \rho^3 \phi(z)}{\frac{W}{A_R} + \left(\frac{A_R}{\pi \rho_t} \right)^{1/4} \left(\frac{v_g}{v_{tg}} \right)^{3/4} \left(\frac{2g}{f_D} \right)^{1/2} \left[(\rho_t - \rho)(\rho - \rho_g)^4 \right]^{1/4}} \quad (39)$$

which, as before, may be solved once $\phi(z)$ is known.

Inasmuch as such an attempt would have required trial and error solutions, and since $\phi(z)$ was itself dependent upon ρ from the diffusion equation, it was decided, so as to finish the report within the millenium, to postulate negligible velocity.

Returning, therefore, to equations (33) and (34), with U_s equal to zero,

$$\text{Riser: } \frac{d\rho}{dz} = -\bar{K} \sqrt{\beta} J_1(2.405 \sqrt{\beta}) \rho^3 \phi(z) \quad (33')$$

$$\text{Down-comer: } \frac{d\rho}{dz} = -\bar{K} \left[J_1(2.405) - \sqrt{\beta} J_1(2.405 \sqrt{\beta}) \right] \rho^3 \phi(z) \quad (34')$$

Concerning oneself with the riser:

$$\frac{d\rho}{\rho^3} = -C \phi(z) dz \quad (\text{where } C \text{ is still a constant})$$

and integrating between proper limits, and normalizing so as to cancel "C".

$$\frac{\int_{\rho_B}^{\rho} \frac{d\rho}{\rho^3}}{\int_{\rho_B}^{\rho_2} \frac{d\rho}{\rho^3}} = \frac{\int_0^z \phi(z) dz}{\int_0^1 \phi(z) dz}$$

or

$$\left(\frac{\rho_B}{\rho}\right)^2 - 1 = \left[\left(\frac{\rho_B}{\rho_2}\right)^2 - 1\right] \frac{\int_0^z \phi(z) dz}{\int_0^1 \phi(z) dz} \quad (40)$$

At this point, the flux and density form may be solved for by combining the diffusion equation, written in difference notation with equation (40). This was done as is explained in somewhat more detail elsewhere.

For present purposes, however, so as to related the change in riser density with that in the downcomer, equation (33') may be written

$$\frac{d\rho}{\rho^2} = -c\rho\phi(z)$$

or

$$\frac{1}{\rho_B} - \frac{1}{\rho_2} = \bar{K}\sqrt{\beta} J_1(2.405\sqrt{\beta}) \int_1^0 \rho\phi(z) dz \quad (41)$$

Now the assumption is made that the flux and density forms in the downcomer are the same as in the riser, but turned end for end. Strictly speaking this is probably not so, however it will serve for an approximation:

In the downcomer then,

$$\frac{1}{\rho_B} - \frac{1}{\rho_f} = \bar{K}H \left[J_1(2.405) - \sqrt{\beta} J_1(2.405\sqrt{\beta}) \right] \int_0^1 \rho\phi(z) dz \quad (42)$$

Dividing (41) by (42), and simplifying:

$$\frac{\rho_2}{\rho_f} = \left(\frac{\rho_2}{\rho_B} - 1 \right) \left[\frac{J_1(2.405)}{\sqrt{B} J_1(2.405\sqrt{B})} - 1 \right] H + \frac{\rho_2}{\rho_B} \quad (43)$$

which relates downcomer density to riser.

(Note: Although again, the introduction of H may not be aesthetically pleasing, still it does serve to relate $\frac{\rho_2}{\rho_f}$ to $\frac{\rho_2}{\rho_B}$ more closely as downcomer height approaches zero.)

Returning to the riser equation and normalizing it,

$$\frac{\frac{1}{\rho_B} - \frac{1}{\rho}}{\frac{1}{\rho_B} - \frac{1}{\rho_2}} = - \frac{\int_1^z \rho \phi(z) dz}{\int_0^1 \rho \phi(z) dz}$$

But the right hand of the equation was found to be very close to a parabola in form, of equation

$$F(z) = -1.1(z-1)^2 \quad (44)$$

Substituting for this value, and simplifying:

$$\frac{\rho}{\rho_f} = \frac{1}{\frac{\rho_2}{\rho_B} + 1.1 \left(\frac{\rho_2}{\rho_B} - \frac{\rho_2}{\rho_B} \right) (z-1)^2} \quad (45)$$

(This equation as is evident is not too accurate close to $z = 0$. However, since eventually only the average densities are required this is not too important a factor.)

In the downcomer, similarly,

$$\frac{\rho}{\rho_f} = \frac{1}{\frac{\rho_2}{\rho_B} + 1.1 \left(1 - \frac{\rho_2}{\rho_B} \right) z^2} \quad (46)$$

The average densities and riser and downcomer are found by integrating $\frac{\rho}{\rho_f}$ over z :

$$\text{Riser: } \frac{\rho_i}{\rho_f} = \frac{\frac{\rho_a}{\rho_f}}{\sqrt{1.1 \left(1 - \frac{\rho_a}{\rho_f}\right)}} \tanh^{-1} \sqrt{1.1 \left(1 - \frac{\rho_a}{\rho_f}\right)} \quad (47)$$

$$\text{Downcomer: } \frac{\rho_o}{\rho_f} = \frac{\frac{\rho_a}{\rho_f}}{\sqrt{1.1 \left(1 - \frac{\rho_a}{\rho_f}\right)}} \tanh^{-1} \sqrt{1.1 \left(1 - \frac{\rho_a}{\rho_f}\right)} \quad (48)$$

Considering the core as a whole, the average density is equal to

$$\frac{\bar{\rho}}{\rho_f} = \frac{\frac{\rho_o}{\rho_f} V_D + \frac{\rho_i}{\rho_f} V_R}{V_D + V_R}$$

where V_D and V_R are downcomer and riser volumes, respectively.

$$= \frac{\frac{\rho_o}{\rho_f} A_D Z_D + \frac{\rho_i}{\rho_f} A_R Z_R}{A_D Z_D + A_R Z_R}$$

Substituting equation (3):

$$\frac{\bar{\rho}}{\rho_f} = \frac{\frac{\rho_o}{\rho_f} (1-\beta) H + \frac{\rho_i}{\rho_f} \beta}{(1-\beta) H + \beta} \quad (49)$$

The remaining item of interest remains the determination of the expression for K .

The flow losses may be considered as being made up of the following:

Acceleration losses: downcomer, down- comer to riser, riser, and riser to downcomer	+	Expansion, or contraction, loss from downcomer to riser	+	180° turn from downcomer to riser	+	Separator region loss
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Friction losses will be considered negligible.

Dividing the core into the appropriate sections, and making use of the mechanical energy equation:

Downcomer:

$$\frac{d\rho}{\rho} + z_R dH + \frac{1}{g} U dU = 0$$

If H is not one, then the fluid leaving the separators with velocity U_f must fall with the acceleration of gravity until it reaches H, whereupon the velocity changes abruptly to U_f again.

Therefore:

$$\int_{P_f}^{P_H} \frac{d\rho}{\rho} + z_R \int_1^H \rho dH + \int_{U_f}^{U_H} \frac{\rho}{g} U dU + \int_{U_H}^{U_f} \frac{\rho}{g} U dU + \text{velocity head loss} = 0$$

If the liquid falls fast enough, no liquid will vaporized, so ρ is constant

$$\begin{aligned} \therefore P_H - P_f + z_R \rho_f (H-1) + \frac{\rho_f}{2g} [U_H^2 - U_f^2] + \frac{\rho_f}{2g} [U_f^2 - U_H^2] \\ + \frac{\rho_f}{2g} [U_H^2 - U_f^2] = 0 \end{aligned}$$

which postulates that all the velocity head energy gain is lost in turbulence at H.

$$U_H^2 = U_f^2 + 2g(1-H)z_R \quad (\text{from acceleration considerations}) \quad (50)$$

$$\therefore P_f - P_H = \Delta P_{f-H} = z_R \rho_f (H-1) + \rho_f (1-H) z_R = 0 \quad (51)$$

From H to 0:

$$\frac{d\rho}{\rho} + z_R dH + \frac{1}{g} U dU = 0$$

$$\int_{P_f}^{P_{B_0}} \frac{d\rho}{\rho} + z_R \int_H^0 \rho dH + \frac{1}{g} \int_{U_f}^{U_{B_0}} \rho U dU = 0$$

From continuity, $W = \rho A_D U$ (where U is mixture velocity)

Also $\int_H^0 \rho dH = -\rho_0 H$

$$\therefore P_{B_0} - P_f - \rho_0 H z_R = -\frac{W}{\rho A_D} (U_{B_0} - U_f)$$

$$\therefore \Delta P_L = P_f - P_{B_0} = \frac{W}{\rho A_D} (U_{B_0} - U_f) - \rho_0 H z_R$$

Again by continuity, $W = \rho_f A_D U_f$

(52)

Downcomer to Riser:

$$\int_{P_{B_D}}^{P_{B_R}} dP + \frac{1}{g} \int_{U_{B_D}}^{U_{B_R}} \rho U dU + \text{"fitting" losses} = 0$$

If it is assumed that the density remains constant, then

$$P_{B_R} - P_{B_D} + \frac{\rho_B}{g} \left[\frac{U_{B_R}^2}{2} - \frac{U_{B_D}^2}{2} \right] + \text{fitting losses} = 0.$$

If $\beta < .5$, then there is a sudden contraction loss, of the form $\rho_B K_1 \frac{U_{B_D}^2}{2g}$ where K depends upon the relative areas of flow, and therefore upon β .³

If $\beta > .5$, the loss becomes an expansion loss, of form

$$\frac{\rho_B}{2g} (U_{B_D} - U_{B_R})^2 \quad 4.$$

The 180° turn loss is of form $\frac{\rho_B}{2g} K_2 U_{B_D}^2$ where $K_2 \cong 3$, from available literature concerning pipe bends.⁵

Therefore, adding all items together

$$\Delta P_{D-R} = \frac{\rho_B}{2g} [U_{B_R}^2 - U_{B_D}^2] + \left\{ \begin{array}{l} \frac{\rho_B}{2g} K_1 U_{B_D}^2 \quad \beta < .5 \\ \frac{\rho_B}{2g} (U_{B_D} - U_{B_R})^2 \quad \beta > .5 \end{array} \right\} + \frac{\rho_B}{2g} 3 U_{B_D}^2 \quad (53)$$

The analysis in the riser is similar to that in the downcomer:

$$\Delta P_R = \frac{\rho_f}{g} \frac{1-\beta}{\beta} (U_2 U_f - U_{B_R} U_f) + \rho_f Z_R \quad (54)$$

Through the separator region, the acceleration and fitting loss analysis becomes somewhat difficult. The integral $\int_{U_f}^{U_2} \rho U dU$ might be divided into three regions; a contraction to the separator inlet, the separator, and the expansion from separator exit to downcomer. It will be assumed that the separator inlet area holds over the separator region and that

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3. Binder, R. C., pg. 107, Fluid Mechanics
 4. Binder, R. C., pg. 103, Fluid Mechanics
 5. Binder, R. C., pg. 106-107, Fluid Mechanics

it is equal to $0.2 \frac{A_T}{\beta} = 0.2 \frac{A_R}{\beta}$.

$$\begin{aligned} \therefore \Delta p &= \frac{1}{g} \left[\rho_2 \int_{U_2}^{U_T} U dU + \frac{W}{A_R} \int_{U_T}^{U_{T+}} dU + \rho_4 \int_{U_{T+}}^{U_4} U dU \right] \\ &= \rho_2 \frac{U_2^2}{2g} \left\{ \frac{\rho_2}{\rho_4} \left[10\beta + \left(\frac{\beta}{1-\beta} \right)^2 - \left(\frac{\beta}{0.2} \right)^2 \right] + \left(\frac{\beta}{0.2} \right)^2 - 1 - 10\beta \right\} \quad (55) \end{aligned}$$

In order that it be possible to obtain the best value of β , an analytical form of the separator region losses must be approximated. It would seem that such losses would depend primarily on the riser velocity and density.

Therefore, $\Delta p_s \cong \frac{\rho_2}{2g} K_3 U_2^2$ (56)

This is admittedly a poor assumption, but, short of experiment, will have to serve.

For evaluation of K_3 , it became necessary to fall back on tentative calculations for a separator region which would have a Δp of ~ 1.5 psi at 40% void fraction and 5 fps velocity at riser top. From this "guesstimation", $K_3 \cong 20$.

Finally, adding (51), (52), (53), (54), (55), and (56)

$$\begin{aligned} \frac{\bar{\rho}}{2g} K U_2^2 &= \frac{\rho_4}{g} (U_{B0} U_4 - U_4^2) + \left\{ \begin{array}{ll} \frac{\rho_B}{2g} K_1 U_{B0}^2 & \beta < 0.5 \\ \frac{\rho_B}{2g} (U_{B0} - U_{BR})^2 & \beta > 0.5 \end{array} \right\} + \frac{\rho_2}{2g} 20 U_2^2 \\ &+ \frac{\rho_B}{2g} 3 U_{B0}^2 + \frac{\rho_B}{2g} (U_{BR}^2 - U_{B0}^2) + \frac{\rho_4}{g} \frac{1-\beta}{\beta} (U_2 U_4 - U_{BR} U_4) \\ &+ \frac{\rho_2}{2g} U_2^2 \left\{ \frac{\rho_2}{\rho_4} \left[10\beta + \left(\frac{\beta}{1-\beta} \right)^2 - \left(\frac{\beta}{0.2} \right)^2 \right] + \left(\frac{\beta}{0.2} \right)^2 - 1 - 10\beta \right\} \end{aligned}$$

Note: The two terms omitted, $\rho_0 Z_R H$ and $\rho_1 Z_R$ are, of course, the driving force.

Employing continuity, and simplifying:

$$\therefore K = \frac{\rho_2}{\rho} \frac{\rho_2}{\rho_B} \left[\left(\frac{\beta}{1-\beta} \right)^2 \left\{ 4 + \frac{\rho_B}{\rho_4} \left[-1 + 10 \frac{(1-\beta)^2}{\beta} - \frac{(1-\beta)^2}{.04} \right] \right\} \right. \\ \left. + \frac{\rho_B}{\rho_2} \left[1 + \left(\frac{\beta}{0.2} \right)^2 - 10\beta \right] - 1 + \left\{ \frac{K_1 \left(\frac{\beta}{1-\beta} \right)^2}{\left(\frac{\beta}{1-\beta} - 1 \right)^2} \right\} + 20 \frac{\rho_2}{\rho} \right] \quad (57)$$

Conclusions:¹

The equations, to be solved in order (43), (47), (48), give the characteristic densities in the reactor, if β and H are chosen (Fig. 10).

With additional solution of (7) and (57), they are (when H is set equal to one) sufficient to determine, for a fixed value of $\frac{\rho_2}{\rho_B}$ (or $\frac{\bar{\rho}}{\rho_4}$), an optimum value of β to give maximum power density. Such a curve, with

$\frac{\rho_2}{\rho_B} = 0.5$ is plotted in Fig. 13. The peak is about $\beta = 0.7$, which value was chosen for succeeding calculations. Incidentally, the value of the separator pressure drop has a greater or lesser influence on the optimum value of β , depending upon its magnitude in comparison with the other losses. It was found later on that K_3 probably should have been of the order of 60. Although a recalculation of optimum β was not undergone, it is believed that the value of .7 for β could not increase more than to .8. However, this is a point that should be examined.

With $\beta = .7$ and $H = 1$, a curve may be drawn showing the variation of power density with mean or average density decrease, the parameter being core height (Fig. 11). However this curve only serves to give an approximate mean density decrease with fixed power density and height,

¹ Note: Thermodynamic properties used were those of H_2O , which should not introduce significant errors.

since separator losses are so vague.

As mentioned, it was discovered that at the chosen power density and core height, the corresponding mean density decrease did not give the required separator pressure drop, which was now ~ 2 psi, due to an error in the previous separator reference calculation used.

Therefore, a different tack was followed of fixing power density (derived from fixed total power and core dimensions) and plotting driving force (refer to discussion following equation (4)) versus mean density decrease and head losses, omitting separator region fitting losses (where U_2 was defined by equation (4)) against mean density decrease. (See Fig. 12). The difference between the curves at any mean density decrease is the allowable separator loss taking slurry weight - 255 grams/liter - into account. In the present case, it was found that a 2 psi drop in a 17.5' x 17.5' core, which was chosen, corresponded to a density decrease of about 30% (instead of 24% as indicated on the power density curve). To adjust the total K to give the proper relations, in accordance with the definitions made, required a K_3 of about 63. This probably would increase β , but, it is believed, not too much with respect to the accuracy of the calculations made. Incidentally, these calculations of actual pressure drop require the consideration of the fuel weight.

It will be noticed that the equations developed also permit analysis of a reactor with external downcomers. The changes that are made are that $\frac{\rho_2}{\rho_3} = \frac{\rho_3}{\rho_4}$, thereby negating equations (43) and (48). (Apparently because of the small amount of vapor formation in the internal downcomer, the optimum β is still .7). An exploratory comparison of the two types was made; total powers were equated as were core average density in the internal downcomer case and riser average density in the external downcomer

case. For such postulations, the result was that the fuel hold-up was slightly more in the external downcomer reactor, but not enough to cause a discard of this type, without further investigation. Thus, the final choice of internal downcomers was based more upon a case of construction basis than a hydrodynamic basis.

An attempt was also made to determine, to some extent, the effect of slip velocity. To this end, equations (33) and (34) were solved, assuming $\beta = 0.6$, $U_s = 1$ fps (which according to Zmola, was a reasonable value at the operating pressure of 1000 psi), and the integral of $\rho\phi$ to be still a parabola in form. The final result was a power density about 7% less than the no-slip case for the same mean density decrease. Of course, the effect of fuel weight was neglected. Again this difference is outside the limits of accuracy of the calculations. However, with the assumptions made, it does indicate a small effect on power removal.

Inasmuch as the reactor may operate at partial load with variable downcomer height, and the equations have included this variability, a set of calculations were made showing the effect upon power of downcomer height, assuming constant D_2O inventory, constant pressure, and continuous criticality; the results are plotted in Figure 14. Of course, as the downcomer height drops, due to the steam's not being totally condensed, the driving force decreases, eventually reaching zero at $H = .66$. By the original assumptions, therefore, which dictated no slip, the power density drops to zero. Manifestly, this is impossible; what actually happens is that natural rise becomes predominant. As a result, from Zmola's equations on natural rise systems, assuming the same downcomer and riser heights and core average density as at the zero point of natural circulation, the power density was calculated (see Fig. 14).

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Surprisingly enough, this power density was even higher than the natural circulation one at full power and full downcomer height.

Because of the many liberal assumptions made in the course of the equation derivations, and the paucity of numbers actually calculated, it is difficult to make a definite conclusion about these results. In fact, it may be that the equations are inadequate to describe such a system variance. However, if they are a fairly true indication of the system, then it would seem that in this case, natural rise has it over natural circulation. (The low value of natural circulation power density, ignoring slip effects, is due to two causes; large separator pressure drop and low mean density decrease; the natural rise power density is the maximum that it can be for it involves the maximum mean density decrease, i.e., 3/11, according to Zmola, the limit on the system.)⁶

Therefore, it would appear that the core is operating under unfavorable conditions, that is, that circulation is enough to reduce vapor time in the riser below that which would result from natural rise, but not enough to cause the increased amount of vapor "manufactured" to overcome this smaller lag.

The question then arises as to whether the system can operate under partial load conditions in this manner. It would seem not, that the additional factor of variable D₂O inventory must be introduced.

Recommendations:

The biggest stumbling block in the whole analysis is that of the flow losses involved in the separator region. As a result, experimental work should be done relating pressure drop to flow rate in a mockup of the

6. Zmola, P. C., RST -13

separator region described elsewhere in this report.

The effect of slip velocity should be investigated more thoroughly, by solving equation (39) or, if possible, setting up more accurate relations involving fuel weight, especially if thorium for breeding (since this item increases the fuel weight enormously) is included. This becomes especially important when one considers the results of the study of variable downcomer height.

One possible aid to the circulating system which was not studied involves the increase in driving force, and therefore, power removal, when the separator region is divorced from the core, being raised above it. Thus, theoretically, density changes take place only in the core; the mixture rises through the connecting portion to the separators; fluid leaves and passes back down through the intermediate section into the core. Thus, there is an increase in driving force without a corresponding core change. This item should be studied.

In particular, it might be profitably applied to the reactor with external downcomers, thus perhaps throwing the balance to this system.

In final analysis, however, it would seem that, for one to be sure of the results, experiments should be performed, with conditions as close to reality as possible. This becomes necessary when one considers the system complexity, which practically defies accurate theoretical analysis.

APPENDIX III

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CONTROL

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APPENDIX III

CONTROL

Part I NATURAL RISE BOILING REACTOR CRITICALITY ANALYSIS

A. Vapor Effect

$$k = \frac{\eta f p}{1 + M^2 B^2}$$

$$M^2 = L_o^2 + \tau = (L_o^2 + \tau_o) \left(\frac{\rho_o}{\rho} \right)^2$$

$$M^2 = \frac{L_o^2 + \tau}{(1 - \nu)^2}$$

$$B^2 = \left(\frac{\pi}{h} \right)^2 + \left(\frac{2.4}{R} \right)^2$$

$$B^2 = \frac{\pi^2 (1 - \nu)^2}{h_o^2} + \left(\frac{2.4}{R} \right)^2$$

$$M^2 B^2 = \frac{L_o^2 + \tau}{(1 - \nu)^2} \left[\frac{\pi^2 (1 - \nu)^2}{h_o^2} + \left(\frac{2.4}{R} \right)^2 \right]$$

$$M^2 B^2 = \frac{\pi^2 (L_o^2 + \tau)}{h_o^2} + \frac{(2.4)^2 (L_o^2 + \tau)}{R^2 (1 - \nu)^2}$$

$$k = \frac{\eta f p}{1 + \frac{\pi^2 (L_o^2 + \tau)}{h_o^2} + \frac{2.4^2 (L_o^2 + \tau)}{R^2 (1 - \nu)^2}}$$

$$\nu = \frac{\text{volume of vapor}}{\text{volume of vapor} + \text{Liquid}}$$

B. Slurry Removal

$$h_o = h_{oo} \frac{V}{V_{oo}}$$

$$V = V_{oo} - \Delta V$$

$$= h_{oo} \frac{V_{oo} - \Delta V}{V_{oo}}$$

$$= h_{oo} \left(1 - \frac{\Delta V}{V_{oo}} \right)$$

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$$k = \frac{\eta_{fp}}{1 + \frac{\pi^2 (L_o^2 + \tau)}{h_{oo}^2 (1 - \frac{\Delta V}{V_{oo}})^2} + \frac{2.4^2 (L_o^2 + \tau)}{R^2 (1 - \frac{\Delta V}{V_{oo}})^2}}$$

C. D2O Removal Only

$$h_o = h_{oo} (1 - \frac{\Delta V}{V_{oo}})$$

$$L_o^2 \approx \frac{D^{D2O}}{\sum a_o}$$

$$\begin{aligned} \sum a_o &= \sum a^F + \sum a^{th} \\ &= \frac{\rho_{F \cdot a} \cdot \sigma_{a^F}}{A_F} + \frac{\rho_{th \cdot a} \cdot \sigma_{a^{th}}}{A_{th}} \end{aligned}$$

$$L_o^2 \approx \frac{D^{D2O}}{\sum a_o} \cdot \frac{V_o}{V} = \frac{\rho_{F \cdot a} \cdot \sigma_{a^F}}{A_F} \cdot \frac{\rho_F}{\rho_o^F} \cdot \frac{\rho_{th \cdot a} \cdot \sigma_{a^{th}}}{A_{th}} \cdot \frac{\rho_{th}}{\rho_o^{th}}$$

$$\begin{aligned} L_o^2 &\approx L_{oo}^2 (1 - \frac{\Delta V}{V_o}) = \frac{\rho_{F \cdot a} \cdot \sigma_{a^F}}{A_F} \cdot \frac{V_o}{V} \cdot \frac{\rho_{th \cdot a} \cdot \sigma_{a^{th}}}{A_{th}} \cdot \frac{V_o}{V} \\ &= \sum a_o \cdot \frac{V_o}{V} \end{aligned}$$

$\tau \approx$ independent of conc.

$f \approx$ independent of conc.

$$p = \exp \left[- (2.86) (10^{-3}) y^{0.747} \right]$$

$$y = \text{conc. Th} = y_o \cdot \frac{V_o}{V} = \frac{y_o}{1 - \frac{\Delta V}{V_o}}$$

$$p = \exp \left[- (2.86) (10^{-3}) \frac{y_o^{0.747}}{\left(1 - \frac{\Delta V}{V_o}\right)^{0.747}} \right]$$

$$k = \frac{\eta f \exp \left[-0.00286 \left(\frac{y_o}{1 - \frac{\Delta V}{V_o}} \right)^{0.747} \right]}{1 + \frac{\pi^2 \left[L_{oo}^2 \left(1 - \frac{\Delta V}{V_o} \right) + \tau \right]}{h_{oo}^2 \left(1 - \frac{\Delta V}{V_o} \right)^2} + \frac{2.4^2 \left[L_{oo}^2 \left(1 - \frac{\Delta V}{V_o} \right) + \tau \right]}{R^2 \left(1 - \frac{\Delta V}{V_o} \right)^2}}$$

Comparison -

20^{ft} I. D. x 12^{ft} h
 $k = 1.045$

15^{ft} I. D. x 9^{ft} h.
 $k = 1.055$

Vol. Slurry to
 return to $k = 1.0$

1500 ft³

644 ft³

Vol. for D₂O
 removal to return
 to $k = 1.0$

800 ft³

275 ft³

Part II NATURAL RISE BOILING REACTOR OPERATING CHARACTERISTICS

Steam Chest Effects

FIG. 44

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Assume system in equilibrium at (1)

- (i) Load drops suddenly.
- (ii) Primary system pressure rises due to excess of core power output over load.
- (iii) Net change of D_2O from liquid to steam raises pressure and reduces net reactivity.
- (iv) Decrease in reactivity reduces core power.
- (v) System arrives at new stable point "2"
- (vi) Similar, but in opposite direction, changes for an increase in load bringing system to new stable point "3"/.

Kinetics of Steam Chest Control

P_c = Core power, BTU/hr.

ΔV = Excess volume of D_2O (liquid) in core over that required for critical at power P .

h_{fg} = Latent heat of evaporation, BTU/lb.

v = Specific volume of liquid.

v_a = Average value of specific volume.

P_r = Power removal BTU/hr.

Initial conditions:

$$\Delta V = \Delta V_0$$

$$\text{at time } t = 0^+$$

$$\Delta V = \Delta V_0$$

$$P_c = P_{c0}$$

$$P_c = P_{c0}$$

$$P_r = P_{r0}$$

$$P_r = P_{r1}$$

Equations:

$$(i) \quad P_c = K \cdot V$$

$$(ii) \quad \frac{d}{dt} \Delta V = - \frac{P_c - P_{r1}}{h_{fg} v_a}$$

$$(i \text{ \& \; } ii) \quad \frac{dP_c}{dt} + \frac{\kappa \cdot v_a}{h_{fg}} P_c = \frac{P_{ri} \cdot v_a \cdot \kappa}{h_{fg}}$$

$$\text{Solution: } P_c = P_{ri} + (P_{co} - P_{ri}) e^{-\alpha t}$$

$$\alpha = \frac{\kappa \cdot v_a}{h_{fg}}$$

$$\text{Example value of } \alpha: \kappa = \frac{3.4 \times 10^9 \text{ BTU/hr}}{800 \text{ ft}^3}$$

$$v_a = 0.0216 \text{ ft}^3/\text{lb}$$

$$h_{fg} = 649 \text{ BTU/lb}$$

$$\alpha = 142 \text{ hrs}^{-1}$$

$$\alpha = 0.0395 \text{ sec}^{-1}$$

$$T = \frac{1}{\alpha} = 25.3 \text{ secs}$$

Corresponding change in steam pressure in chest:

$$v^s = \text{Specific volume of steam}$$

$$V^s = \text{Steam chest volume}$$

Solve for specific volume -

$$v^s = \frac{v_o^s}{1 + (P_c - P_{ri}) \left(\frac{v_o^s}{h_{fg} v^s} \right)}$$

Corresponding steam pressures (to v^s) may be found in the steam tables.

Diagram illustrating a natural circulation loop system, likely for a reactor. The system consists of a primary loop (V₁) and a secondary loop (V₂), connected to a steam generator (V₄) and a condenser (V₃).

The primary loop (V₁) is connected to the steam generator (V₄) and the condenser (V₃). The secondary loop (V₂) is connected to the primary loop and the condenser (V₃).

Key parameters and labels in the diagram include:

- h_1 : Height of the primary loop from the condenser to the steam generator.
- h_2 : Height of the secondary loop from the condenser to the steam generator.
- h_3 : Height of the condenser (V₃) from the primary loop.
- h_4 : Height of the steam generator (V₄) from the primary loop.
- A_1 : Area of the primary loop.
- A_2 : Area of the secondary loop.
- $A_3 = \frac{a}{\cos \theta}$: Area of the condenser (V₃).

A. Conservation of mass (Total D_2O inventory in primary system constant

$$h_3 \rho_3 - h_2 \rho_2 = \alpha_1 \frac{\rho_3 v^2}{2g}$$

$$h_3 - \frac{\rho_2}{\rho_3} h_2 = \alpha_1 \left(\frac{v_s}{L} \right)^2 \quad \text{or} \quad h_3 = \alpha_1 \left(\frac{P v_s}{L} \right)^2 + \frac{\rho_2}{\rho_3} h_2$$

$$\frac{V_2}{V_0} M_0 = A_2 h_2 + (1-f) V_1 + \frac{V_4}{V_0} V_4 + \alpha_1 A_3 \left(\frac{P V_s}{L} \right)^2 + A_3 \frac{\rho_2}{\rho_3} h_2$$

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$$\Delta p = \rho_2 h_2 - h_1 \rho_2 (1-f) = \rho_2 (h_2 - h_1 + h_1 f)$$

$$\frac{\rho v^2}{2g} = \frac{\rho_2 (1-f) v^2}{2g} = \alpha_2''' \rho_2 (h_2 - h_1 + h_1 f)$$

$$v^2 = \alpha_2''' \frac{h_2 - h_1 + h_1 f}{1-f}$$

$$v = \alpha_2' \sqrt{\frac{h_2 - h_1 + h_1 f}{1-f}}$$

$$P = \frac{f v A L}{V_s} = \frac{A f L}{V_s} \alpha_2' \sqrt{\frac{h_2 - h_1 + h_1 f}{1-f}}$$

This assumes
all vapor separated
in separators

$$P = \frac{\alpha_2 L f}{V_s} \sqrt{\frac{h_2 - h_1 + h_1 f}{1-f}}$$

Solving for h_2

$$h_2 = \frac{1-f}{f^2} \left(\frac{V_s P}{\alpha_2 L} \right)^2 + (1-f) h_1$$

Substitute h_2 from II into I

$$\begin{aligned} \frac{V_e}{V_{e0}} M_o &= A_2 \frac{1-f}{f^2} \left(\frac{V_s P}{\alpha_2 L} \right)^2 + A_2 (1-f) h_1 + (1-f) V_1 + \frac{V_e}{V_s} V_4 \\ &+ \alpha_1 A_3 \left(\frac{V_s P}{L} \right)^2 + A_3 \frac{P_2}{P_3} \frac{(1-f)}{f^2} \left(\frac{V_s P}{\alpha_2 L} \right)^2 + A_3 \frac{P_2}{P_3} (1-f) h_1 \end{aligned}$$

Solving for P:

$$P = \frac{\alpha_2 L}{V_s} \sqrt{\frac{\frac{V_e}{V_{e0}} M_o - \frac{V_e}{V_s} V_4 - h_1 (A_2 + A_3 \frac{P_2}{P_3}) - V_1 + f [V_1 + h_1 (A_2 + A_3 \frac{P_2}{P_3})]}{(A_2 + A_3 \frac{P_2}{P_3})(1-f) + \alpha_1 \alpha_2^2 A_3 f^2}}$$

where α_2 & α_1 are defined as

$$\alpha_1 = \left[\frac{h_3 - \frac{P_2}{P_3} h_2}{\left(\frac{P V_s}{L} \right)^2} \right]_o$$

$$\alpha_2 = \left[\frac{V_s P}{L f} \sqrt{\frac{1-f}{h_1 f}} \right]_o$$

Subscript o means
evaluated at full
power design point

Similarly h_2 vs f may be found from I and II

$$h_2 = (1-f) \left\{ \frac{h_1 + \frac{V_e}{V_{e0}} M_o - \frac{V_e}{V_s} V_4 - h_1 (A_2 + A_3 \frac{P_2}{P_3}) - V_1 + f [V_1 + h_1 (A_2 + A_3 \frac{P_2}{P_3})]}{(A_2 + A_3 \frac{P_2}{P_3})(1-f) + \alpha_1 \alpha_2^2 A_3 f^2} \right\}$$

Definition of Symbols

P = Total power removal
 L = Latent heat of vaporization
 v_s = Specific volume of steam
 v = Specific volume of liquid
 f = Average void fraction in
 M_0 = Total equiv-volume of liquid D_2O at design point
 h_1 = Riser height
 h_2 = Downcomer liquid height
 A_2 = Downcomer area
 A_3 = Condensate return area
 ρ_2 = Density in downcomer
 ρ_3 = Density in Cond. return
 V_1 = Riser volume
 V_4 = Steam space volume
 α_1 = Constant of proportionality
 α_2 = Constant of proportionality

Assumptions

- (1) Design point given
- (2) No voids in downcomer
- (3) Turbulent flow in core and in steam pipes
- (4) No slip of bubbles relative to liquid

APPENDIX IV

CHEMICAL PROCESSING

PART I. DERIVATION OF THE DECAY HEAT VS. COOLING TIME CURVES

It was necessary to obtain the f.p. heat evolution when a continuous process is employed. Only batch data was available.

The continuous process was envisioned as a series of large number of discrete batches. Each small batch was irradiated and then allowed to cool in the reactor. The contribution, Q , of this batch to the heating of a sample withdrawn at any time from the reactor will be given by:

$$Q = \left[\begin{array}{l} \text{The heat evolution of batch 1 at time } t \text{ after irradiation} \\ \text{The probability that it is not withdrawn until time } t \end{array} \right] \times \left[\begin{array}{l} \text{The probability that it is withdrawn in a time interval } \Delta T \end{array} \right]$$

$$Q_1(t) = H_1(t) e^{-At} \Delta T A$$

H_1 = heating of batch 1 at time t after irradiation*

A = process rate

ΔT = time period for withdrawal of sample (taken as 5 days in calculation)

Then the total heating, Q_c , at any time after removal from the reactor will be given by:

Days cooling	0	5	10	α
(after removal from the reactor)	Q_0	Q_5	Q_{10}	----
	Q_5	Q_{10}	Q_{15}	----
	Q_{10}	Q_{15}	Q_{20}	----

$$Q_c = \sum_0^T Q_i(t) + \sum_5^{T+5} Q_i(t) + \sum_{10}^{T+10} Q_i(t) + \sum_{\alpha}^{T+\alpha} Q_i(t)$$

*C.F. 54-4-62

Since our reactor contains a large amount of Th as well as U, the decay heat of Pa at the equilibrium Pa concentration was added to the f.p. heat to obtain the curve shown in Fig. 28.

PART II. DECAY HEAT PRODUCTION IN IRRADIATED SLURRY

For 300 day continuous processing cycle. ν conc. = 4.2 g and equilibrium Pa conc. = 0.280 g/liter.

(a) Shortly after removal from reactor.

From graph # I, heating = $35.8 \frac{\text{watts}}{\text{liter}}$

$$\frac{\Delta T}{\text{Time}} = \frac{35.8 \times 60 \frac{\text{sec}}{\text{min}}}{41.8 \frac{\text{watt} \cdot \text{sec}}{\text{gm-cal}}} \times 1000 \text{ gms} = 0.520^\circ\text{C/min}$$

(b) Three days after removal.

heating = $32.3 \frac{\text{watts}}{\text{liter}}$

$$\frac{\Delta T}{\text{time}} = \frac{32.3 \times 60}{4.18 \times 1000} = 0.465^\circ\text{C/min}$$

- (c) Assuming that all the heat generated dissipated in the solids when all the D_2O has been removed and there is no heat transfer to surroundings, for 250 g ThO_2 /liter (design condition) and 3 day cooling

$$\frac{\Delta T}{\text{time}} = \frac{32.3 \times 60}{4.18 \times 250 \times 0.057} = 32.5^\circ\text{C/min}$$

$$C_p \text{ of } \text{ThO}_2 = 0.057 \text{ cal/g } ^\circ\text{C}$$

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REACTOR POWER	DIMENSIONS, VOLUME	FLUX X 10 ⁻¹⁴		U ²³³ CONCENTRATION		Th CONCENTRATION		BREEDING GAIN		REACTOR INVENTORY			
(MW)	POWER DENSITY	(grams/liter of D ₂ O											
	VOID FRACTION	Pa in ⁽¹⁾	Pa out ⁽¹⁾	Pa in	Pa out	Pa in	Pa out	Pa in	Pa out	U ²³³ kg		D ₂ O (lbs)	Th (kg)
										in	out		in out
500	17.9 x 16.5	2.32	2.36	0.900	.885	50	50	-.285	-.259	72.3	71.1	182,000 ⁽²⁾	4,020
	118,000 liters	1.30	1.31	1.60	1.59	99	99	-.122	-.105	129	128	(144,000)	7,960
	4.23 kw/liter	.804	.805	2.59	2.58	164	164	-.018	-.012	208	207		13,200
	(6.21 kw/l D ₂ O in core)	.493	.495	4.23	4.22	250	250	.029	.030	340	339		20,100
	(4.91 kw/l D ₂ O)	.193	.193	10.6	10.8	500	500	.105	.105	870	870		40,200
	0.320												
	15.4 x 14.2	1.10	1.13	3.23	3.13	164	164	-.180	-.137	151	146	143,000 ⁽³⁾	7,600
	74,800 liters	.705	.720	5.07	4.97	250	250	-.093	-.070	236	231	(132,000)	11,700
	6.66 kw/l core	.287	.289	12.5	12.4	500	500	.010	.013	580	577		23,300
	(10.8 kw/l D ₂ O in core)	.160	.161	22.4	22.3	700	700	.040	.040	1000	1000		32,600
	(10.0 kw/l D ₂ O)												
	0.378												

(1) "Pa in" refers to the case in which Pa is at equilibrium in the reactor (i.e., it is not removed in the processing operation)

"Pa out" refers to the case in which Pa is removed completely in the chemical processing operation

(2) includes 16,000 ft³ of D₂O vapor at 1000 psi in vapor space plus 44 ft³ of D₂O liquid in condensate return line

(3) includes 9,000 ft³ of D₂O vapor plus 44 ft³ of D₂O liquid

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REACTOR INVENTORY CHARGE				PROCESS INVENTORY CHARGE				TOTAL INVENTORY CHARGE			
U ²³³	D ₂ O	Th		U ²³³	D ₂ O	Th		U ²³³	D ₂ O	Th	
in	out	in	out	in	out	in	out	in	out	in	out
.165	.162	.661	.008	.138	.037	.007		.300	.698	.015	
.294	.292	.016		.021	.008	.430	.682	.024			
.474	.472	.026		.013	.008	.610	.674	.034			
.775	.773	.040		.008	.007	.911	.669	.047			
1.98	1.96	.080		.003	.006	2.12	.664	.036			
.344	.333	.520	.015	.138	.016	.006	.471	.536	.021		
.537	.527	.023		.010	.006	.683	.530	.029			
1.32	1.31	.047		.004	.005	1.45	.524	.052			
2.28	2.28	.065		.002	.004	2.42	.522	.069			

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PROCESSING RATE	U ²³³ MAKEUP CHARGE	D ₂ O MAKEUP CHARGE	PROCESSING CHARGE	TOTAL COST (\$/kw-hr)	TOTAL COST & 80% load
kg Th/day					
Thorex			Thorex		
57.4	.902	.277	Thorex \$5.00		
63.1	.365		Thorex \$5.50		
64.1	.042				
60.2	-.115				
46.8	-.866				
52.0	.476	.218			
51.0	.268				
40.7	-.045				
31.7	-.153				

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Pa in	Pa out	Pa in	Pa out
\$50/kg	\$5.50 kg		
2.99	2.28	3.74	2.85
2.65	1.88	3.31	2.35
2.53	1.74	3.16	2.18
2.63	1.88	3.29	2.35
3.43	2.85	4.29	3.56
2.44	1.80	3.05	2.25
2.42	1.79	3.03	2.24
2.76	2.26	3.45	2.83
3.52	3.12	4.40	3.90

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