AN EVALUATION OF THE REACTOR CONCEPT VULCAIN

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I. INTRODUCTION

This report has been prepared in response to a request from M. Jean Van der Spek, Managing Director of Belgonucleaire, for an evaluation of the reactor concept "Vulcain," which is under development by his company.

The information on this concept on which this review has been based is contained in the following reports, all dated March, 1961:

VN-61-301: Conception et Viabilité Economique
VN-61-303: Etudes D'Orientation
VN-61-305: Description Succincte et Caractéristiques Principales du Projet
VN-61-309: Méthodes et Calculs.

The information in these reports was supplemented by a series of conferences with M. Pierre Maldague held on March 31 - April 5, in Cambridge, Massachusetts, U.S.A.

This reactor concept has been developed for two possible applications:

1) a reactor for ship propulsion with a heat rating of 65 MW, to develop 23,700 shaft horsepower, and

2) a reactor for a central-station electric power plant with a heat rating of 650 MW and a net electric power generation of 195 MW.

Since investigation of the reactor for ship propulsion has been carried much further by Belgonucleaire than the reactor for central-station power, most of the comments of this report refer to the ship reactor. In addition, however, some estimates of the suitability of the Vulcain concept for central-station power are given also.

It is important to make clear what has been included in our evaluation and what has not been. The design bases and assumptions have been reviewed, but no detailed check has been made of the numerical accuracy of all data employed. Spot checks have been made of Belgonucleaire's nuclear, thermal and economic calculations, but a complete check of all design calculations has not been attempted. Such specific mechanical designs as have been proposed by Belgonucleaire have been noted to see what ideas are being considered, but a numerical check of the mechanical design was not considered worthwhile, since this project is still in the conceptual design stage. The control and safety aspects of the concept were considered, but a complete hazards review could not be given for the same reason.

Our main effort has been to obtain an understanding of the advantages and disadvantages of the Vulcain reactor concept and its suitability for ship propulsion and central-station power generation.

II. CONCLUSIONS

2.1 General Conclusions

The essential features of the Vulcain concept to which our general conclusions apply are these:

a) Control of reactivity by variable moderation.

b) Separation of core lattice into two regions, an annular region containing fuel and coolant, surrounding an inner region containing moderator and safety rods.

c) Use of continuous change in moderator temperature to adjust reactor heat output to match turbine demand and to compensate for changes in reactivity due to xenon and relatively short-term changes in fuel composition.

d) Use of discontinuous change in H_2O content of D_2O moderator and coolant from an initial value of around 5% to a final value of around 40%, to compensate for changes in fuel composition.

e) Use of slightly enriched uranium as fuel.

For ship propulsion, the Vulcain concept, thus defined, is a feasible and attractive reactor concept. Compared with a pressurized-water reactor such as might be developed by improvement of the reactor proposed for the nuclear ship Savannah, the Vulcain concept has the advantages of

a) Higher mean power density.

b) A simpler control system.

c) Better neutron economy.

These advantages should lead to lower shaft horsepower cost through lower capital and lower fuel cycle costs.

The conclusion of report VN-61-301, page 38, that the annual cost of a ship propelled by a Vulcain reactor of the design presently contemplated is 12% less than the cost of a ship propelled by an improved pressurized-water reactor appears reasonable. It is possible, however, that the criticisms of the present specific Vulcain design given in Section 2.3 below may add enough to the cost of this reactor to reduce its apparent cost advantage. Nevertheless, since the Vulcain reactor has definite technical advantages, it is probable that it will show some cost advantage after the engineering design has been refined.

The fact that fuel in the Vulcain reactor remains in one place during its entire life in the reactor is an advantage compared with the advanced PWR, in which fuel must be moved radially inward twice during life.

For central-station power generation, the advantages of the Vulcain concept compared with competing reactors are less clear. Large, land-based pressurized-water reactors can use features not applicable to ship propulsion reactors to improve power density and neutron economy, notably use of multizoned loading with radial shuffling and use of soluble poison to control long-term changes in reactivity. Another competitor, the Spectral Shift Controlled Reactor (SSCR) under development by Babcock and Wilcox[1] is capable of giving as high power density and as good neutron economy as the Vulcain reactor; although its control of reactivity by continuous change of H_2O/D_2O ratio is impractical on ship-board, it should be feasible on land. Until Belgonucleaire completes more detailed engineering of a large central-station Vulcain reactor, it is difficult to establish that it will have an economic advantage compared with an improved pressurized-water reactor or the SSCR.

2.2 Favorable Comments on Specific Vulcain Design

The specific design proposed for the core of the Vulcain reactor is ingenious and efficient, and is well adapted to attaining the twin objectives of high power density and good neutron economy. Use of moderator-filled space inside fuel elements for safety rod insertion is a good way of maximizing the worth of the rods.

Moment-to-moment control of reactivity by change of moderator temperature, if proved by experiment to be dependable, is a most desirable means for reducing flux distortion due to partially inserted control rods.

2.3 Criticisms of Specific Vulcain Design

Some features of the specific design proposed for the Vulcain reactor which need revision and further engineering effort, and which may add somewhat to the size and cost of the reactor, will now be listed. More detailed discussion of these points will be given in the later sections of the report cited in parentheses.

<u>Reduction of Maximum Heat Flux</u> (Section A). Our thermal analysis of the distribution of coolant and cladding temperature and heat flux down the hottest channel of the Vulcain ship reactor shows that the heat flux will reach 70% of boiling burnout at the top of the fuel channel. The highest value thus far used in pressurized-water reactors is 50%[2]. We believe the Vulcain margin to be too close and recommend modification of the design as by reducing flux peaking at the top of the core, increasing the fuel-cladding surface, or reducing coolant temperature. Each of these measures will add to power cost. In addition, a study should be made to be sure that the maximum allowable cladding surface temperature will not be exceeded if coolant flow is accidentally cut off.

<u>Prevention of Collapse of Moderator Tubes</u> (Section B). In the present design, moderator within the reactor is separated from coolant by zircaloy tubes 146 mm in outside diameter by 2 mm in wall thickness. The space inside these tubes is used for insertion of safety rods to shut down the reactor. Pressure balance between coolant and moderator is established by interconnecting coolant and moderator piping outside of the reactor. If the moderator piping outside of the reactor should rupture, the moderator pressure in the reactor would fall suddenly and the thin-walled moderator tubes would collapse. This would obstruct insertion of the safety rods and

might lead to an uncontrolled runaway of the reactor, since the moderator temperature would drop on loss of pressure, with possible increase in reactivity. It is imperative that the engineering design of the reactor be modified to prevent collapse of these pressure tubes under all conceivable circumstances. Possible changes, both of which have disadvantages, are: 1) increase in moderator tube thickness coupled with provision of pressure relief diaphragms between coolant and moderator inside the reactor or 2) establishment of pressure balance within the reactor, by allowing moderator to mix freely with coolant after flowing through each moderator tube.

Novel Mechanical Features (Section C). Two novel mechanical features of the proposed Vulcain design which contribute to its compactness and economy, but which may give trouble unless carefully engineered and given extensive service tests are: 1) the telescoping control rod drives, and 2) the intricate passages through which moderator enters and leaves each moderator tube at the bottom of the core. The control rod drives should be tested for drop time under reactor conditions and for service life. The design of moderator inlet and outlet passages should be reviewed having in mind the possibility of dirt fouling or obstructing the passages or damaging the seals.

It is recognized that these novel features are not an essential part of the Vulcain concept.

2.4 Use of Moderator Temperature to Control Reactivity

Since the use of moderator temperature to control reactivity is one of the most novel and potentially valuable features of the Vulcain concept, we have examined the feasibility of this method of control in some detail. We conclude (Section D) that the method is sound and workable, provided the time constants of the reactor thermal system and coolant and moderator cooling circuits are properly balanced. A careful transient analysis should be made of the dynamic response of these systems, as part of the detailed engineering design, and the control system should be exhaustively tested on a prototype of the reactor and its heat-removal and control systems.

2.5 Fuel Burnup (Section E)

The Vulcain fuel-cycle analysis has made the simplifying and somewhat optimistic assumption that fuel composition remains spatially uniform during irradiation, whereas actually the fuel is burned out more heavily at regions of high power density and less heavily elsewhere. Since time did not permit us to make a more accurate calculation of burnup for the Vulcain reactor, we have estimated the magnitude of the error by adapting available results for a pressurized-water reactor to the Vulcain case. For the ship reactor we estimate that for a desired average burnup of 45,000 MWD/ton the feed enrichment, maximum burnup and fuel-cycle cost would be changed as follows from those given in VN-61-301:

Source of estimate	Project Vulcain, VN-61-301	This report, Section E
Composition assumption	Uniform	Non-uniform
Enrichment, % U-235	7	7.4
Maximum burnup, MWD/ton	45,000	50,800
Fuel cycle cost, centimes/CVh	14.2	14.6

Although tests of uranium oxide fuel clad in stainless steel are beginning to confirm that burnups of 45,000 to 50,000 MWD/ton can be attained without excessive radiation damage at the design maximum linear specific power of 40 kw/m specified for Vulcain, the possibility that further experiments may require reduction in maximum burnup should be kept in mind. This was considered in the Vulcain reports, where it was shown that the cost increase would not be great.

III. RECOMMENDATIONS

The Vulcain principle is a promising reactor concept for ship propulsion which merits further investigation.

In view of the novelty of the Vulcain concept and the large amount of experimental development work needed to check out thoroughly its technical innovations, exploitation of this concept will be an expensive undertaking. The proponents of the reactor concept should determine that the returns from this concept are great enough to justify the expense and effort of the development.

Assuming that a decision is made to proceed further with this concept in Belgium, future work along the following lines is recommended:

1) The present Vulcain design should be revised to (a) eliminate the close approach to boiling burnout at top of core, and (b) to preclude collapse of moderator tubes under all conceivable accidental circumstances.

2) Since the results of a hazards analysis often have an important effect on reactor design, a detailed hazards analysis should be made of the Vulcain design. This analysis should include consideration of the effects of loss of coolant flow, loss of control drive actuating pressure, rupture of coolant or moderating piping, mal-operation of

the control system, and other possible accidents on the integrity and safety of the reactor.

3) A critical experiment should be run on a Vulcain-type core under conditions approximating as closely as possible the dimensions, isotopic compositions, pressure and temperatures proposed for the ship reactor. The proposal to reassemble the fuel rods for the BR-3 reactor into fuel elements similar to those specified for Vulcain, to be tested in the BR-3 pressure vessel, should provide an adequate check of the reactor-physics techniques used in designing the Vulcain core, especially if detailed measurements are made of the distribution of neutron flux and energy within the core.

4) Models of the telescoping control rod drives and other novel and critical mechanical components should be built and tested exhaustively under conditions approximating as closely as possible their actual service in Vulcain. The effect of radiation on these components should be investigated experimentally.

5) Before a Vulcain reactor is constructed on shipboard, a full-scale, land-based prototype should be built and tested thoroughly. This prototype should duplicate in all important respects the reactor vessel, control drives, coolant and moderator systems, and reactor control system, but need not include the steam turbine if a steam dump system of the same characteristics is provided. The performance of the

moderator-temperature reactor control system should be given special attention during operation of this prototype, since it is the most novel, most critical, and potentially the most valuable feature of Vulcain.

6) A more accurate calculation should be made of the burnup to be expected in the Vulcain reactor when the non-uniform composition distribution that will develop during irradiation is taken into account.

A project including the foregoing phases aimed at completing the development of the Vulcain reactor for ship propulsion would be a constructive and worthwhile means of unifying Belgian activity in the nuclear reactor field.

Section A

HEAT FLUX DISTRIBUTION IN VULCAIN REACTOR

Hot Channel Factors

Hot channel factors are of primary importance in determining the maximum thermal power output of a reactor. There is as yet no sure and accurate method for determining the values of these hot channel factors. Since one great advantage of the Vulcain reactor is the low value of the nuclear hot channel factors, an estimate of these factors was made by hand calculation, using a two-group energy model. The engineering hot channel factors have also been reviewed and commented upon on pages 18 and 19.

Nuclear Hot Channel Factors.

The reactor considered is the N-1 described in report VN-61-305 using the nuclear core and reflector characteristics for the beginning of life conditions given in the "Note de Calcul No. 36," page 36.4. The fuel and moderator regions have first been homogenized using a cylindrical unit cell with the moderator in the inner zone of a radius of 7.7 cm and the fuel and coolant in the outer zone of an outside radius of 10.4 cm. The moderator utilization factor f defined as

$$f = \frac{V_0 \Sigma_{a0} \bar{\phi}_0}{V_0 \Sigma_{a0} \bar{\phi}_0 + V_1 \Sigma_{a1} \bar{\phi}_1} \qquad \text{where} \quad \begin{array}{l} 0 = \text{moderator} \\ 1 = \text{fuel + coolant} \end{array}$$

was first calculated for the fast and thermal groups, using the diffusion approximation and slowing down density in both regions and for both energy groups. An expression for f was found as

$$\frac{1}{f} = \frac{1 + \frac{V_1 q_1}{V_0 q_0}}{\left(\frac{q_1}{q_0}\frac{\Sigma_{a0}}{\Sigma_{a1}} - 1\right)\left[F + \frac{D_0}{D_1}\left(\frac{K_0}{K_1}\right)^2\left(\frac{V_0}{V_1}\right) E\right]^{-1} + 1}$$

where the functions F and (E-1) are respectively the well known flux disadvantage and excess absorption functions. The ratio of the slowing down density q_1/q_0 was tentatively estimated to be equal to the ratio of the water concentration in both regions for the fast group and equal to the ratio of the slowing down cross-sections for the thermal group. Both regions were then homogenized using the expressions

$$\frac{\bar{\phi}_1}{\bar{\phi}_0} = \frac{V_0 \Sigma_{a0}}{V_1 \Sigma_{a1}}\left[\frac{1}{f} - 1\right]$$

$$\bar{\Sigma} = \frac{\Sigma_0 + \Sigma_1 \frac{\bar{\phi}_1 V_1}{\bar{\phi}_0 V_0}}{1 + \frac{\bar{\phi}_1 V_1}{\bar{\phi}_0 V_0}}$$

The result of this computation is given in the table below.

<u>Fast Group</u> $\frac{\bar{\phi}_1}{\bar{\phi}_0} = 0.523$

Fast Cross Sections	Core	Reflector
D_1 cm	1.550	1.835
Σ_1 cm^{-1}	0.00975	0.009198
$\Sigma_{s\ell}$ cm^{-1}	0.00566	0.009198
Σ_f cm^{-1}	0.00271	

<u>Thermal Group</u> $\frac{\bar{\phi}_1}{\bar{\phi}_0} = 0.322$

Thermal Cross Sections	Core	Reflector
D_2 cm	0.908	0.871
Σ_2 cm^{-1}	0.02765	0.000680
Σ_f cm^{-1}	0.0478	

The spatial distribution of the fast and thermal flux was then calculated in a two-region spherical reactor. The radius of the spherical core was determined as follows. An equivalent bare cylindrical reactor was calculated for the Vulcain reactor. From the one-group slab geometry, reflector savings of 10.80 cm for the fast group and 10.5 cm for the thermal group were found. A closer estimate of the reflector saving was obtained by multiplying the thermal reflector saving, 10.5 cm, by a factor of 1.2 to account for the fact that the one-group diffusion model underestimates the reflector effect. Using a core radius of 48.55 cm, a height of 100 cm, and a reflector saving of 12.6 an equivalent bare cylindrical core having a radius of 61.15 cm and a height of 125.2 cm was obtained. This bare core was then converted to an equivalent bare sphere using the equivalence of the geometrical buckling in the two geometries

$$\left(\frac{\pi}{R'}\right)^2 = \left(\frac{2.405}{R_1'}\right)^2 + \left(\frac{\pi}{H}\right)^2$$

An equivalent bare spherical reactor of a radius of 67.3 cm was thus arrived at. By subtracting from this radius the reflector saving of 12.6 cm, an equivalent reflected spherical reactor was obtained having a core radius of 54.7 cm and a reflector thickness of 30 cm.

From the two-group diffusion theory and using the nuclear characteristics tabulated above, a spatial distribution of the flux was obtained.

$$\Phi_1 = \frac{\sin 0.0386r}{r} - 1.172 \times 10^{-5} \frac{\sinh 0.1908r}{r}$$

$$\Phi_2 = 0.195 \frac{\sin 0.0386r}{r} + 1.23 \times 10^{-5} \frac{\sinh 0.1908r}{r}$$

as well as a power distribution in the core

$$P = \Sigma_{f,1}\,\phi_1 + \Sigma_{f,2}\,\phi_2$$

$$P = 1.203\,\frac{\sin 0.0386r}{r} + 5.56 \times 10^{-5}\,\frac{\sinh 0.1908r}{r}$$

This power distribution gives the following overall macroscopic nuclear hot channel factor

$$F_q = \frac{P_{max}}{P_{av.}} = 1.436$$

This factor compares favorably with the value of 1.24 x 1.24 = 1.55 used in VN-61-309 for the thermal analysis.

For subsequent calculations in cylindrical geometry we have estimated a radial hot channel factor to be $F_R = (1.436)^{2/3} = 1.271$. Using this radial macroscopic hot channel factor and the overall F_q in the central plane (X-Y geometry) of 1.55 given on page 23 of report VN-61-309 we have evaluated a central microscopic hot channel factor F_g

$$F_g = \frac{1.55}{1.271} = 1.22$$

The overall nuclear hot channel factor at the center of the core was then calculated as

$$F_q(\text{center}) = 1.436 \times 1.22 = 1.75$$

To calculate the hot channel factor at the core-reflector interface we have to recognize the fact that the soft neutron spectrum in the reflector diffusing back into the core will appreciably raise the power at the interface. This effect will be very significant in an undermoderated reactor and has not been so far accounted for. We therefore have assumed that this effect will increase both the macroscopic and microscopic hot channel factor by 20%, i.e., a total of 44%. The previously calculated power at the interface being 76% of the power at the core center, the overall hot channel factor at the interface is thus found to be

$$F_q(\text{interface}) = 1.436 \times 0.76 \times 1.44 \times 1.22 = 1.91$$

Engineering Hot Channel Factors

The engineering hot channel factors used in report VN-61-309, page 57, are identical to the factors used by Westinghouse in pressurized reactor design until recently. These hot channel factors have now been revaluated in the light of some recent measurements and are reproduced below from YAEC-106 (A.1).

Fuel Rod Characteristics	$F_{\Delta H}$	F_q	F_θ
1. Variation in fuel diameter	1.002	1.003	1.003
2. Variation in fuel material density	1.027	1.05	1.05
3. Variation in enrichment	1.011	1.022	1.022
4. Effect of variation in fuel rod diameter, pitch and bowing	1.097	--	1.134
Coolant Flow Characteristics			
1. Flow distribution in plenum chamber	1.07	--	1.07
2. Cooling effectiveness	1.05	--	1.05
	1.28	1.08	1.37

The main change is in Item 4 which has been reduced by about 10%. It should be pointed out, however, that although the effect of tube bowing on $F_{\Delta H}$ is small (9.7%), the evaluation of the DNB ratio should be made using minimum tube pitching, maximum tube diameter and reduced flow area due to bowing. All these local effects will not greatly affect the channel exit temperature but will appreciably influence the burnout heat flux calculations.

The statistical analysis outlined on page 67 of report VN-61-309 to determine the maximum fuel temperature uses a correct approach but should be modified in view of the following. The $F_{\Delta H}$ factors have been determined from measured average deviation from a nominal value and hence represent an uncertainty of σ (instead of 3σ) on the thermal temperature rise. The factors F_θ and F_q on the other hand have been determined as the difference between maximum and minimum values and should be regarded as representing an uncertainty on the calculated values θ and q of about 2σ (instead of 3σ). (See reference YAEC-106 loc. cit.).

Burnout Study at the Outlet of the Central Channel

Since the Vulcain reactor has a power density at the core reflector interface comparable to the power at the center of the core, it is apparent that the ratio of maximum heat flux to burnout heat flux will be greater at the coolant outlet end of the core (upper interface in the present configuration). The power mapping in the core is, however, not sufficiently refined at present to determine whether the worst condition for burnout is at the upper end corners or at the upper end of a central channel. For the present study, we have deliberately chosen the central channel where the power density is known with less uncertainty than at the corner. It is nevertheless strongly recommended that the upper end burnout conditions be checked when thermal peaking at the core reflector interface has been investigated. For this analysis, we have used the core described in report VN-61-305, a net thermal power of 65 MW with 3% losses from the moderator, and with 94.2% of the heat generated in the fuel.

The total thermal power is therefore

$$65 \times 1.03 \times 3413 \times 10^3 = 2.285 \times 10^8 \text{ BTU/hr}$$

The total number of fuel elements is

$$181 \times 18 = 3258 \text{ elements}$$

The total power per element is

$$7.03 \times 10^4 \text{ BTU/hr}$$

A total mass flow of coolant of 699.6 kg/s uniformly distributed over 3258 elements gives a mass flow per element of

$$M_{av.} = \frac{699.6 \times 3600 \times 2.205}{3258} = 1705 \text{ lbs/hr-channel}$$

A channel has an equivalent diameter of 8.5 mm, hence a mass flow rate of $G_{av} = 2.58 \times 10^6$ lbs/hr-ft^2.

Using a radial nuclear F_q of 1.271 and an average microscopic nuclear hot channel factor of $\frac{1.22 + 1.44}{2} = 1.34$, we obtain the total heat input into a central channel of

$$7.03 \times 10^4 \times 1.271 \times 1.34 = 1.20 \times 10^5 \text{ BTU/hr-elements}$$

Using an average specific heat of the coolant of 1.35 BTU/lb-$^\circ$F, the temperature rise through the coolant is found to be 52.1°F. Applying an engineering hot channel factor of $F_{\Delta T} = 1.392$, the possible rise becomes

$$\Delta T(\text{center}) = 52.1 \times 1.392 = 72.6^{\circ}F$$

Using an inlet temperature of 556°F, the outlet temperature is 628.6°F with a corresponding enthalpy of h_{BO} = 660.1 BTU/lb.

The total heat transfer area of an element 1 meter long is 0.288 ft² and the corresponding average heat flux

$$\Phi_{av} = 0.942 \times 7.03 \times 10^4 / 0.288 = 2.30 \times 10^5 \text{ BTU/hr-ft}^2$$

The possible maximum heat flux at the outlet is obtained by applying a nuclear hot channel factor of 1.91 and an engineering factor of 1.08.

$$\Phi_{max}(\text{center outlet}) = 2.30 \times 10^5 \times 1.91 \times 1.08 = 4.75 \times 10^5 \frac{\text{BTU}}{\text{hr-ft}^2}$$

The burnout heat flux at the center outlet is calculated using the Bettis correlation given in WAPD-188[A.2], page 47.

$$\frac{\Phi_{BO}}{10^6} = 0.182 \left(\frac{H_{BO}}{10^3}\right)^{-2.5} \left(1 + \frac{G}{10^7}\right)^2 e^{-0.0012\ L/D}$$

With the values obtained above, H_{BO} = 660.1 BTU/lb, $G = 2.58 \times 10^6$ lb/hr-ft², $L/D = 1000/8.5 = 117.7$, the burnout heat flux is

$$\Phi_{BO} = 7.28 \times 10^5 \text{ BTU/hr-ft}^2$$

and the DNB ratio

$$\frac{\Phi_{max}}{\Phi_{BO}} = 0.652$$

The uncertainty in the burnout flux due to channel geometry, non-uniform heating, transient conditions, pressure effects are of the order of 20%. The uncertainty in power distribution in the core at the moment are at least of the order of 20%. In adverse circumstances the DNB ratio may therefore still come very close to 1.0 with the present design, thus leaving no margin for power, coolant temperature or flow variations. All pressurized water reactors under study at the present time have a DNB ratio smaller than 0.50[2]. A maximum DNB ratio of 0.66 would probably be acceptable when determined from burnout experimental data on the Vulcain or similar lattice and an accurate power mapping at the core interface obtained from experiments or multi-thermal groups nuclear calculations.

Section B

PREVENTION OF COLLAPSE OF MODERATOR TUBES

In Section 2.3, two suggestions were made for modifying the present Vulcain design to ensure that the tubes separating moderator and coolant would not collapse if the internal moderator pressure were accidentally reduced:

1) Increase in moderator tube thickness, coupled with provision of pressure relief diaphragms between coolant and moderator inside the reactor, which would break before a pressure difference great enough to collapse the tubes could develop, or

2) Maintenance of pressure balance within the reactor by allowing moderator to mix freely with coolant after flowing through each moderator tube.

Of these two suggestions, the second seems preferable to us.

Disadvantages of the first suggestion are the increased cost and increased loss of neutrons with heavier tubes, and the extreme difficulty of replacing or maintaining relief diaphragms in the core.

Two advantages of the second suggestion are 1) it would remove all possibility of tube collapse and 2) it would greatly simplify the manifolding of moderator inlet and outlet passages at the bottom of the reactor, a point discussed further in Section C, following. Disadvantages of this suggestion are 1) if the moderator flow rate were kept at the present Vulcain figure, it would reduce thermal efficiency or 2) if the moderator flow rate were reduced, the action of moderator temperature change on reactivity might be so greatly delayed that the effectiveness of the control system would be impaired. This is discussed in Section D.

Since neither of these suggestions is ideal, we feel that more thought should be given to means for absolutely preventing moderator tube collapse. This must not be allowed to occur under any circumstances whatever.

Section C

CORE MECHANICAL DESIGN

The core mechanical design of the Vulcain reactor presents a difficult problem for the following reasons:

1) The moderator and coolant have to follow separate paths.

2) Heat flow from the coolant to the moderator is to be minimized.

3) The moderator and coolant volume in the pressure vessel is to be kept small in order to reduce inventory cost. All these difficulties have been recognized in the preliminary design of the reactor and ingeniously solved (report VN-61-303).

At this stage of the design, the reliability of such a system is however very much questioned and there is no doubt that this system if ultimately adopted will require extensive full scale testing.

We might point out at the present time some of the difficulties foreseen:

1) Both the moderator distribution tubes and plates at the bottom of the core and the control rod drive mechanism at the top are intricate devices which although fully tested will still require maintenance during operation. These mechanisms are enclosed in the pressure vessel which renders

the maintenance problem very difficult in case of failure. This would be particularly serious in the case of the moderator distributors since the entire core would have to be removed for the maintenance of these parts. Access to the control rod drives although not so difficult would still require the removal of the top of the pressure vessel by remote control, a difficult operation.

2) The spring-loaded insertion of the control rods appears to be adequate but some estimate of the rate of descent of the rod in water should be made in order to estimate the scram time. An accurate evaluation of the scram time is necessary for hazards evaluation studies, and it is important to keep this time as small as possible.

3) The removal of the control rod from the core relies on pressure differential between the moderator and the interior of the telescopic device. This means of removal would be invalidated if the seal ring in the telescopic device should leak. This defect is sufficient to put the reactor out of operation and it is recommended that some independent means of control rods removal be provided or that the number of seal rings be increased.

4) The pressure line leading to the control rod drives has a very tortuous path and the seal numbered 23 in Figure 91 of report VN-61-303 should be avoided.

5) It is possible that during the removal of a fuel bundle some scale or deposit would set on the seal between moderator and coolant resulting in appreciable flow exchange between the two circuits at different temperatures. This possibility should be avoided since it would reduce the effectiveness of the moderator temperature control on reactivity.

It is realized, however, that the above mechanical difficulties can be overcome or altogether avoided without affecting the basic Vulcain concept.

Section D

VULCAIN REACTOR DYNAMICS

General Remarks

In this section we examine briefly some of the dynamic aspects of the Vulcain reactor. First, we present a general analysis of the reactor core without the associated thermal power conversion equipment. We show from this analysis that the proposed reactor concept has very good inherent stability characteristics. Second, we discuss the controllability of the Vulcain plant through moderator temperature changes. This discussion is only qualitative both because of lack of time and because Belgonucleaire's design had not become sufficiently specific to warrant a quantitative study.

Reactor Core Dynamics

Fast reactivity perturbations which are not initiated by a change in the load cannot be controlled by the moderator loop because of the time lag associated with this loop. It is important then to investigate the stability and response of the reactor core without the thermal energy conversion equipment.

The reactor model is as shown in Fig. 1. The moderator temperature is essentially constant and mixing and pipe effects are ineffective.

The reactor can be represented by the following set of equations:

$$\frac{dP}{dt} = \frac{\rho - \beta}{\Lambda} P + \sum \lambda_i C_i \qquad (1)$$

$$\frac{dC_i}{dt} = \frac{\beta_i}{\Lambda} P - \lambda_i C_i \qquad (2)$$

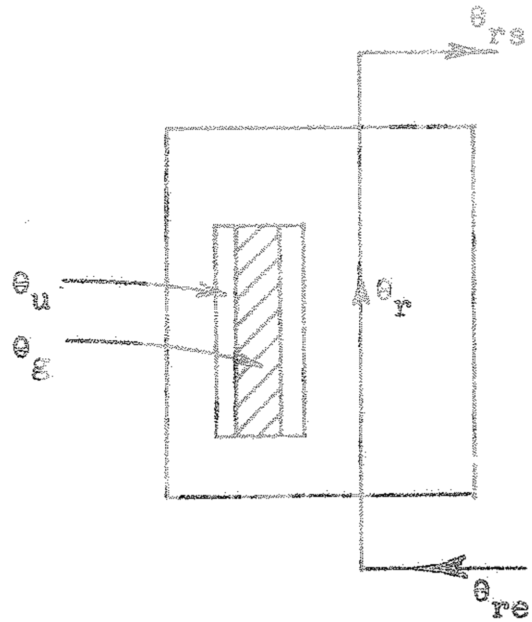

Figure 1. Reactor Core Schematic for Fast Transients

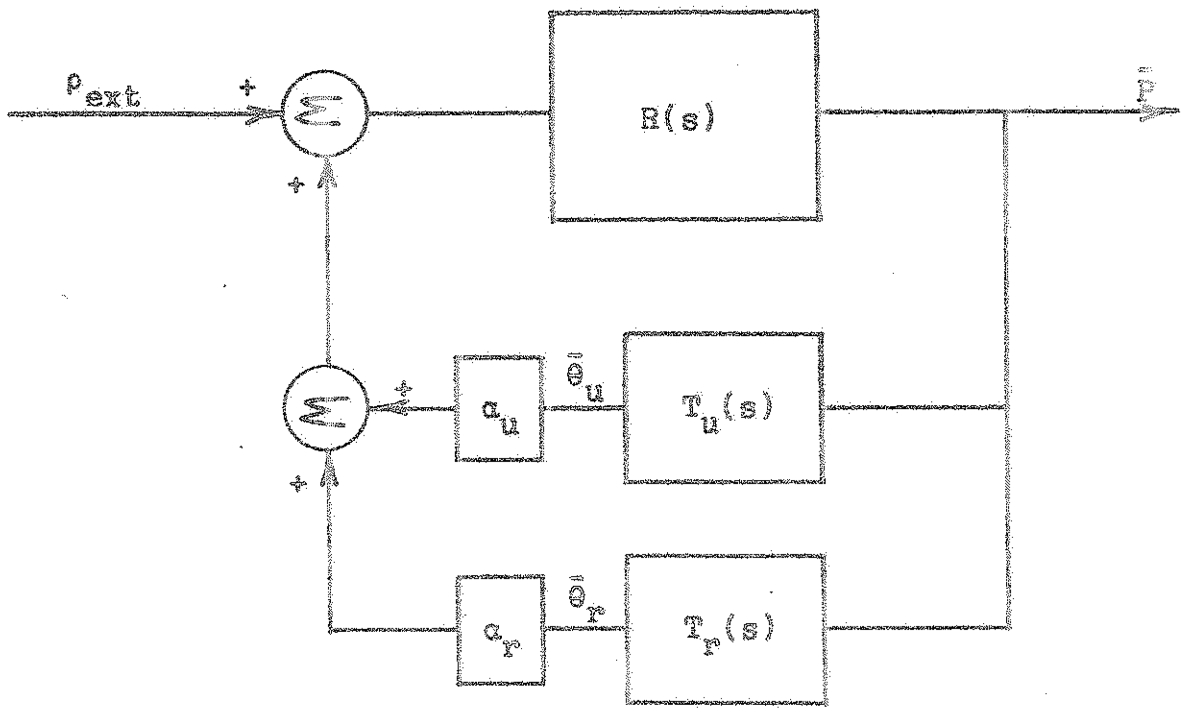

Figure 2. Reactor Core Block Diagram

$$\frac{d\theta_u}{dt} = \alpha_1(P - P_o) - g_1(\theta_u - \theta_g) \tag{3}$$

$$\frac{d\theta_g}{dt} = g_2(\theta_u - \theta_g) - g_3(\theta_g - \theta_r) \tag{4}$$

$$\frac{d\theta_r}{dt} = g_4(\theta_g - \theta_r) + \alpha_2(P - P_o) - 2g_5(\theta_r - \theta_{re}) \tag{5}$$

$$\rho = \rho_{ext} - \alpha_v(\theta_u - \theta_{uo}) - \alpha_r(\theta_r - \theta_{ro}) \tag{6}$$

where

$\alpha_1 = \frac{0.95}{M_u} = 2.18 \times 10^{-3}$ °C/kW-sec

$\alpha_2 = \frac{7.4 \times 10^{-3}}{M_r} = 0.96 \times 10^{-5}$

$g_1 = \frac{4k_u S_u}{M_u r_u} = 0.63$ sec^{-1}

$g_2 = \frac{4k_u S_u}{M_g r_u} = 3.22$ sec^{-1}

$g_3 = \frac{A_r}{M_g} = 39$ sec^{-1}

$g_4 = \frac{A_r}{M_r} = 4.34$ sec^{-1}

$g_5 = \frac{W_r C_r \gamma_r}{M_r} = 4.25$ sec^{-1}

all other symbols as defined in Note de Calcul No. 33 - Dec. 28, 1960.

For a stability and transient response analysis, applicable for small reactivity perturbations, Eqs. (1-6) can be linearized and the coolant input temperature assumed constant. Using incremental quantities and Laplace transforms, the reactor block diagram is as shown in Fig. 2 where

$$R(s) = \frac{P_o}{\Lambda s} \frac{1}{1 + \frac{1}{\Lambda} \sum_{1}^{6} \frac{\beta_i}{s + \lambda_i}}$$

$$T_u(s) = \frac{\alpha_1\left[(s+g_2+g_3)(s+g_4+2g_5) - g_3g_4\right] + \alpha_2 g_1 g_3}{(s+g_4+2g_5)\left[s^2+s(g_1+g_2+g_3)+g_1g_3\right]}$$

$$T_r(s) = \frac{\alpha_1 g_2 g_4 + \alpha_2\left[(s+g_1)(s+g_2+g_3) - g_1g_2\right]}{(s+g_4+2g_5)\left[s^2+s(g_1+g_2+g_3)+g_1g_3\right]}$$

P_o = steady state power level

This system has a very good inherent stability for all the contemplated values of the coefficients of reactivity α_u and α_r. In particular when

$\alpha_u = -10^{-4}\delta k/^oC \quad \alpha_r = -2 \times 10^{-4}\delta k/^oC \quad \Lambda = 4 \times 10^{-5}$ sec

The characteristic equation of the system is

$$Q(s) = 1 + 5.5 \times 10^{-3}\,\frac{P_o}{s}\,\frac{1}{1+\frac{1}{\Lambda}\sum_1^6 \frac{\beta_i}{s+\lambda_i}}\,\frac{(s+8.7)(s+45.7)}{(s+0.58)(s+12.84)(s+42.27)} \qquad (7)$$

Using the fast fission delayed neutron data, as recommended by Keepin (NSE, Dec. 1960), the root locus of Eq. (6) is as shown in Fig. 3. It is evident that the reactor is absolutely stable for all operating power levels. All other values of the coefficients of reactivity result in a similar characteristic locus and therefore an equally stable reactor.

The preceeding stability analysis has been based on a linearized model. Actually the nonlinear model is also absolutely stable. To see this more clearly, neglect delayed neutrons[(D.1)] and lump the cladding with the fuel. The conservative reactor model is now

$$\frac{dP}{dt} = \frac{\rho}{\Lambda}P \qquad (8)$$

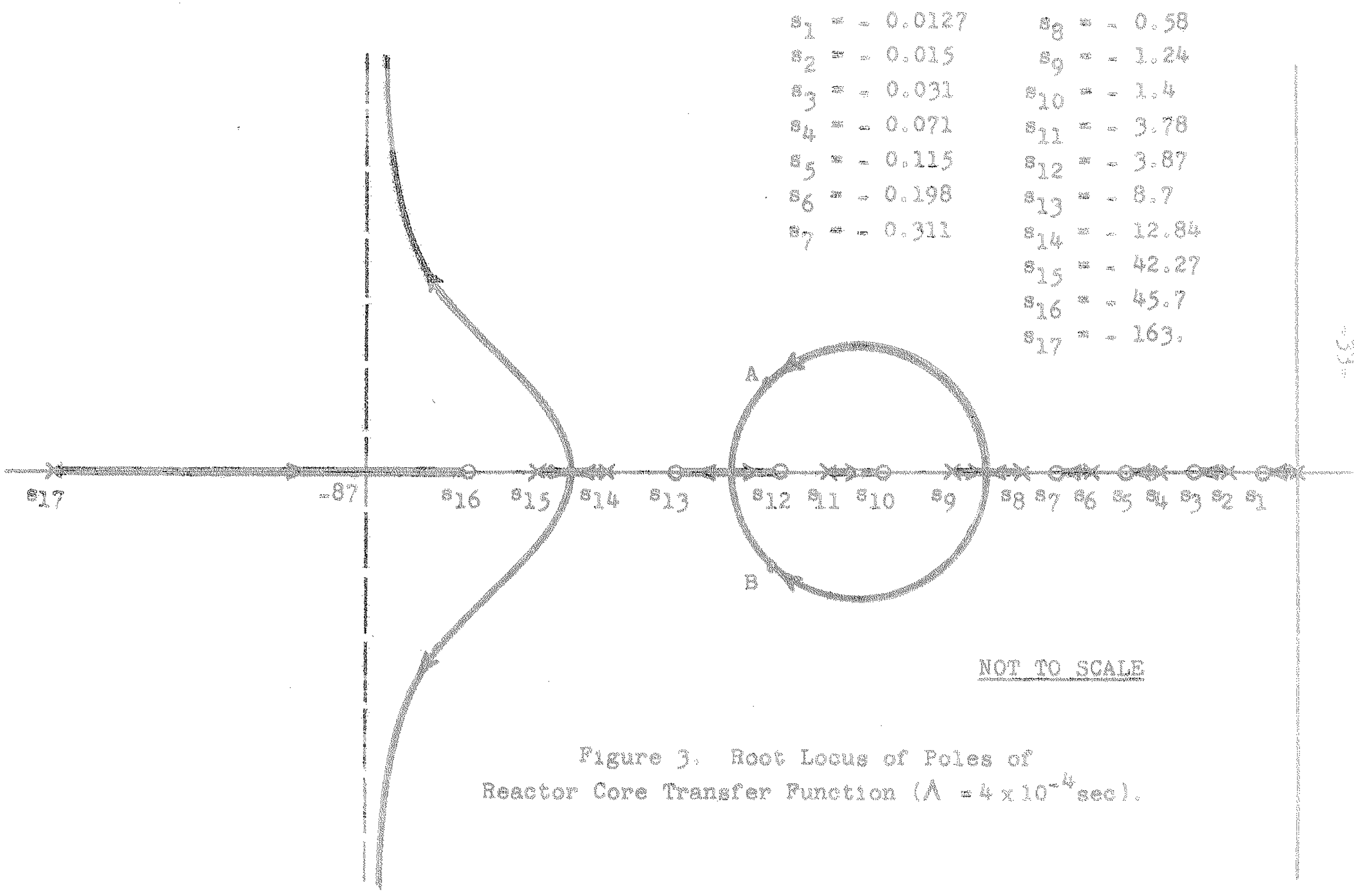

Figure 3. Root Locus of Poles of Reactor Core Transfer Function ($\Lambda = 4 \times 10^{-4}$ sec).

$$\frac{d\theta_u}{dt} = \alpha_1'(P - P_o) - g_1'(\theta_u - \theta_r) \tag{9}$$

$$\frac{d\theta_r}{dt} = \alpha_2'(P - P_o) + g_4'(\theta_u - \theta_r) - 2g_5'\theta_r \tag{10}$$

An analysis of this model by a geometric interpretation of Liapunov's second method (D.2) shows that the reactor is absolutely stable for all negative values of α_u and α_r.

The transient response of the reactor core is also good. This fact can be best appreciated by inspection of the root locus (Fig. 3). The characteristic roots (poles of overall transfer function) that are near the origin (long time constants) are very close to the zeros of the transfer function, therefore the corresponding residues will be small. The characteristic roots that are to the far left side of the locus (time constants much smaller than 0.1 sec.) both have small residues and decay fast. Effectively the reactor behaves like a second order system whose poles correspond to the loop of the locus (typical points A - B). The time constant of the second order system is of the order of a few tenths of a second and the damping is quite high. The transient response is smooth and without overshoots.

<u>Plant Dynamics</u>

The controllability of slower and larger perturbations (due to load changes) through moderator temperature changes is an attractive and conceptually feasible scheme.

The preliminary design calculations by Belgonucleaire have not advanced enough as yet to allow a quantitative evaluation of the method at this stage.

From a qualitative standpoint it is evident that the success of the proposed method for regulation purposes depends on the time constant of the moderator circuit. This time constant must be compatible with the time constant of the anticipated load changes and the time constant of the coolant circuit. If the time constant of the moderator loop is long, load changes will be reflected on the core through the coolant loop resulting in excessive fuel and coolant temperature changes.

It is recommended that the moderator loop circulating time be chosen small and a careful transient analysis be performed to establish the practicality of the method. It is also recommended that the reactor description for this analysis be based on a distributed parameter model because the moderator loop introduces pure time lags, the phase shifts of which are not usually adequately described by simple lag transfer functions.

Section E

FUEL CYCLE COSTS

Because the lower fuel cycle cost of the Vulcain reactor relative to an improved pressurized water reactor is such an important factor in the comparison between the two reactors, the assumptions behind the fuel cycle cost analysis given on pages 28-31 of VN-61-301 were examined in some detail. The first five parts of this section deal with the following points of this fuel cycle analysis:

- E.1 Reactivity to override xenon
- E.2 Assumption of uniform fuel composition
- E.3 Loss of heavy water
- E.4 Interest on heavy water investment
- E.5 Interest on fuel

Part E.6 shows that the fuel-cycle cost comparison between the two reactors is presented fairly in VN-61-301.

E.1 Reactivity to Override Xenon

In calculating the burnup that could be obtained from fuel of a specified enrichment, Report VN-61-309 assumed that the fuel would be left in the reactor until its reactivity dropped to zero while the reactor was operating steadily at full load. Near the end of fuel life, if the reactor were shut down completely for several hours, xenon would poison the reactor to such an extent that it would not be possible to operate it at full power. Instead, to start the reactor it would be necessary to lower the coolant temperature and operate at reduced load for several hours until the xenon had been burned out and decayed. The assumption that this limitation on operating flexibility would be acceptable should be reviewed. If it is concluded that reactivity to override xenon under all conditions is required, fuel-cycle costs would be increased somewhat. It is understood, however, that fuel life in the PWR was computed on the same assumption, namely that reactivity to override xenon would not be required, so that the comparison between the two reactors is not affected.

E.2 Assumption of Uniform Fuel Composition

The procedure used to calculate burnup in the Vulcain reactor described in Report VN-61-309 made the simplifying and somewhat optimistic assumption that fuel composition remains spatially uniform during irradiation, whereas actually the fuel is burned out more heavily in regions of high power density and less heavily elsewhere. At the start of irradiation, the ratio of peak power density (averaged over a lattice cell) to power density averaged over the entire core is 1.24 x 1.24 = 1.54. For this reason, the average burnup that will actually be ovtained in Vulcain will be somewhat less than was calculated and the maximum burnup that fuel will experience in regions of peak power density will be somewhat greater than the average burnup calculated.

Since time did not permit us to make a more accurate calculation of burnup for the Vulcain reactor, we have estimated the error in the calculated maximum and average burnups and the fuel-cycle cost from results of burnup calculations for the Yankee pressurized water reactor, which took into account non-uniform changes in fuel composition, which were available at MIT[E.1]. For the present report the calculations for the Yankee reactor were extended to a case in which it was assumed that the fuel composition remained uniform during burnup, as was done in the Vulcain calculations. Since the two sets of calculations for the Yankee reactor used the same nuclear and cost calculation assumptions, the differences in burnups and costs found for the two assumptions regarding composition changes may be used as a starting point in estimating the error introduced by the assumption of uniform composition in Vulcain. Since, in the Yankee reactor, the ratio of peak power density (averaged over a lattice cell) to power density averaged over the entire core is 2.70, it was necessary to adjust the Yankee results to the value 1.54 characteristic of Vulcain. This was done by linear interpolation. The results of the Yankee calculations for the three power density ratios (PDR) are given in Table E.1.

Table E.1

RESULTS OF FUEL CYCLE CALCULATIONS FOR YANKEE REACTOR

Enrichment, a/o U-235	2.876	3.441	4.383	5.592	6.452
Uniform Composition (PDR=1.0)					
Average burnup, MWd/ton	2,930	12,220	27,020	44,540	56,020
Maximum burnup, MWd/ton	2,930	12,220	27,020	44,540	56,020
Fuel cycle cost, mills/kwhr	18.49	5.39	3.76	3.48	3.52
Non-uniform Composition (PDR=2.7)					
Average burnup	1,654	8,684	20,507	35,381	45,420
Maximum burnup	4,507	17,907	35,043	53,313	64,446
Fuel cycle cost			3.35	3.12	
Non-uniform Composition (PDR=1.54) (obtained by interpolation of above)				a)	b)
Average burnup	2,520	11,090	24,930	41,640	52,630
Maximum burnup	3,435	14,030	29,580	47,340	58,710
Fuel cycle cost			3.49	3.24	
Increase in fuel cycle cost, mills/kwhr			0.14	0.12	

An average burnup of 45,000 MWd/ton is desired for the Vulcain reactor. The necessary corrections to the Vulcain burnup calculations can be obtained by interpolating to an average burnup of 45,000 MWd/ton (at PDR = 1.54) between the entries in the above table marked a) and b).

Average burnup (at PDR = 1.0)	48,050 MWd/ton
Maximum burnup (at PDR = 1.54)	50,800 MWd/ton
Increase in fuel cycle cost	0.116 mills/kwhr

The inference drawn from these results is that to obtain an average burnup of 45,000 in Vulcain with a power density ratio of 1.54, it is necessary to use such an enrichment that in a calculation at a power density ratio of 1.0, the average burnup will be 48,050. Report VN-61-308, Figure 8, shows that an enrichment of 7.4% would be needed to obtain this average burnup.

The increased fuel cycle cost, estimated from the above, is 0.116 x 5 x 0.736 = 0.43 centimes/CVh.

E.3 Loss of Heavy Water

Report VN-61-301 assumed that the loss of heavy water from the Vulcain reactors would be 1% of their inventory per year. Since there is no experience on the operation of heavy water reactors at a pressure of 150 kg/cm^2, this assumption cannot be checked directly. It is pertinent to note, however, that the loss rate of heavy water from the Savannah River reactors has been 3%/yr[E.2]. These operate at much lower pressure than Vulcain, but change fuel oftener. Table E.2 compares the absolute and percentage loss rates assumed for the Vulcain reactors with those used by the Canadian and duPont groups for the heavy-water power reactors they have been developing. Since both these groups have had experience in operating heavy-water reactors at lower pressures, it is felt that their estimates of loss from a power reactor should be reliable. It will be noted that both the absolute and relative loss rates assumed by these groups are appreciably higher than has been assumed for Vulcain.

Table E.2

COMPARISON OF ESTIMATES OF
LOSS RATE OF HEAVY WATER FROM NUCLEAR REACTORS

Reactor	CANDU	duPont study	Vulcain Central	Vulcain Ship
Reference	(E.3)	(E.4)	VN-61-301	
Megawatts, thermal	728	790	650	65
electric	200	206.6	195	--
D_2O inventory, kg	169,000	295,000	45,000	8,350
Est. loss rate, kg/yr	3,940	5,800	450	83
% of inventory/yr	2.3	2	1	1

We think that a loss rate of 3% per year would be more reasonable for the Vulcain reactors, in view of their relatively low D_2O inventory. This would increase the fuel cycle cost for the Vulcain ship reactor by

$$\frac{8350 \text{ kg} \times \frac{0.02}{\text{yr}} \times \frac{\$61.6}{\text{kg}} \times 5000 \frac{\text{centimes}}{\$}}{23.690 \text{ CV} \times 8760 \frac{\text{hr}}{\text{yr}} \times 0.75} = 0.33 \text{ centimes/CVh}$$

E.4 Interest on Heavy Water Inventory

In the Vulcain fuel-cycle cost estimate, it was assumed that the interest charges on heavy water and on uranium fuel inventory would each be 4% per year. In computing nuclear power costs for United States reactors, interest on fuel is charged at 4% per year, because an arrangement to rent fuel at this charge can be worked out with the U.S. Atomic Energy Commission, but interest on heavy water is charged at the same rate as is used for non-depreciating assets, since heavy water for power reactors must be purchased and cannot be rented from the U.S. AEC. Since the fuel and heavy water for the Vulcain reactor will probably be obtained from the U.S. AEC, we think that the 4% annual charge on fuel value used by Vulcain is appropriate, but we think that interest on the value of heavy water should be charged at the same rate as working capital, i.e., 9%. If this be done, the contribution of heavy water inventory to the cost of power in the ship reactor would be increased from 0.44 to 0.99 centimes/CVh.

E.5 Interest on Fuel

We are unable to check the contribution to power cost in a PWR ship reactor due to interest on the initial charge of U-235, item 1a) of Table 2.3, page 30, of VN-61-301. Our calculations of this interest contribution for the PWR and the Vulcain reactor with INOX cladding are summarized in Table E.3 and compared with the Vulcain report.

Table E.3

POWER COST DUE TO FUEL INVENTORY

Reactor (75% load factor)	PWR	Vulcain
Burnup, B, MWd/ton	15,000.5	45,000
% U-235	5.5	7.0
Reactor charge, I, tons U	3.68	1.250
Unit value, U, \$M/ton	0.77037	1.028
Thermal power, MW	65	65
Horsepower, CV	22,100	23,690
Mean U throughput, tons/yr = $\frac{65\text{MW} \times 0.75 \times 365 \text{ d/yr}}{\text{B MWd/ton}}$	1.19	0.396
U holdup for 1.3 yr, tons	1.55	0.515
Total U holdup, H, tons	5.23	1.765
Contribution to power cost, centimes/CVh = $\frac{0.04\text{UH} \times 5000}{(\text{CV}) \times 8760 \text{ hr/yr} \times 0.75}$	5.55	2.34
VN-61-301, page 30	4.57	2.39

E.6 Combined Effect of Changes in Fuel Cycle Cost

The combined effect of these changes would be to increase the fuel cycle cost of the Vulcain reactor by:

Non-uniform fuel	0.4 centimes/CVh
Increased loss of D_2O	0.3
Higher interest on D_2O	0.6
Total	1.3

The fuel cycle cost for Vulcain changed by this amount is 15.5 centimes/CVh.

Because of E.5, above, the fuel cycle cost for the PWR is increased by 1.0 to 22.0 centimes/CVh.

The overall effect is to reduce the difference between the PWR and Vulcain by 0.3 centimes/CVh. This comparison, however, does not take into account a disadvantage of the PWR in that it would have to be shut down two times to move fuel during fuel life.

Taking all these factors into account, one may conclude that the fuel cost saving of the Vulcain reactor for ship propulsion compared with the PWR is presented fairly in VN-61-301.

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