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Some Aspects of the Feasibility of a $10^{16}$-Flux Reactor

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by

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I. Introduction

With the operation of the HFIR, HFR, and SH-2, and with the design of the Franco-German and British high flux reactors, a new generation of research reactors exists which produce fluxes in the $10^{15}$/$\text{cm}^2\text{sec}$-range. This is a considerable improvement over the conventional research reactors with fluxes of about $10^{14}$. This achievement was obtained by higher performance, and now the development seems to be close to the limit of present engineering techniques. However, at the International Seminar on Intense Neutron Sources held in Santa Fe in September 1966, it was pointed out that there is a continuous need for even higher neutron fluxes to be used in nuclear and solid state physics applications. Though there is a large class of experiments for which a steady state flux of $10^{15}$/$\text{cm}^2\text{sec}$ is sufficient; and other experiments which can be made with a peak flux of $10^{16}$/$\text{cm}^2\text{sec}$ obtained from a repetitively pulsed reactor, there are a number of experiments which require a steady state flux of $10^{16}$/$\text{cm}^2\text{sec}$ or even more $\left(10^{17}\right)$. A few examples are given below:

1. Studies of asymmetries in the $B$-decay of polarized neutrons

   This is the best available measure of most of the fundamental parameters of the weak interaction and also furnishes an important test of time reversal invariance $\left(\frac{1}{2}\right)$.

2. Studies of neutron-neutron interaction

   Since this interaction rate increases as the square of the flux whereas the background increases linearly with the flux, a higher flux than presently available is required for an acceptable signal-to-background ratio.

3. In the field of neutron capture $\gamma$-rays, measurements of internal conversion spectra with a high resolution magnetic spectrograph and all measurements on materials with small capture cross sections.

4. In the field of fission physics, experiments where magnetic analysis is used to separate the fragments of a given mass.

5. For investigations of condensed matter, (a) single crystal diffraction measurements where sample sizes are small. In particular, these are studies of organic crystals or biological molecules. (b) Measurements of the inelastic scattering of neutrons which cannot be well adapted to the conditions of a repetitively pulsed reactor.
Of course, these examples cannot illustrate completely the great improvement which would be achieved with a $10^{16}$-flux reactor. A large variety of new experiments, for instance with very cold neutrons or with polarized neutrons, would be possible and will be invented once the necessary high flux is available. Moreover, all experiments could be improved in resolution, or in signal-to-background ratio, or in the time required for the measurements.

Another important application is the production of transplutonium isotopes. Since the production rate increases with a high power of the flux, the incentive to increase the flux is particularly high in this case.

In view of this interest in obtaining higher neutron fluxes, a preliminary investigation was made in order to determine the engineering problems and the economic implications of a $10^{16}$-flux reactor. This reactor is also of interest for comparison with a spallation type neutron source, such as the Canadian project ING [4].

A flux of $10^{16}$/cm$^2$ sec can be obtained within present techniques with a large dilute heavy water moderated reactor operating at about 1000 MW, as the experience with the high flux charge at Savannah River indicates [5]; possibly even higher flux at higher power may be attainable. However, a much better ratio of flux to power and a better beam quality (ratio of thermal to fast flux) can be obtained from a reactor with a small compact undermoderated core surrounded by a D$_2$O-reflector which contains the beam tubes.

The purpose of this investigation was to study the possibilities and limitations of such a concept, and a beam research reactor was taken as an example. Some calculations were also made for an isotope reactor, and are given in the Appendix. Since the work was stimulated by design calculations for the Franco-German High Flux Reactor, some design features of this reactor were maintained, such as the annular core with a one ring HFIR-type fuel element, the beryllium plug in the center and the pressure barrel between core and D$_2$O-reflector. The design parameters were pushed to the utmost limits and some reasonable extrapolations beyond current engineering practices were assumed. Only the basic features of the reactor were studied, and such problems as safety, control, radiation damage, and engineering details were not considered. For reasons of simplicity all calculations were made for a reactor without beam tubes.
II. General

In this paragraph the materials and operating conditions chosen will be described, and the reasons for the particular choices will be given.

1. Coolant

As mentioned in Par. I, the reactor considered here has a very compact core. Thus, for the total power necessary to obtain a flux of $10^{16}/\text{cm}^2\text{sec}$, a very high power density is required. Three coolants, $\text{H}_2\text{O}$ above the critical pressure, sodium, and $\text{H}_2$-gas, were considered for the thermodynamic calculations. These calculations were made to determine, for the same power density, which coolant gives the lowest maximum fuel clad temperature.

The results showed that, for the conditions assumed, the higher maximum temperatures were for Na and $\text{H}_2$; hence supercritical $\text{H}_2\text{O}$ was chosen as the coolant. Compared to $\text{H}_2\text{O}$, the following comments apply to the other two coolants:

i) $\text{H}_2$ - higher specific heat, but lower density;

ii) Sodium - higher heat transfer coefficient, but higher inlet temperature and lower specific heat.

Nuclear calculations were also made for a $\text{D}_2\text{O}$-cooled reactor. While the "rendement" 1) for this reactor was better than that of an $\text{H}_2\text{O}$-cooled reactor of the same dimensions, this advantage was overcome by a lower $k_{\text{eff}}$ for the $\text{D}_2\text{O}$-reactor.

2. Coolant Conditions

At the high $\text{H}_2\text{O}$-velocities and small coolant channels necessary with the high power density, there is a pressure drop of about 50 Atm across the core. Hence an inlet pressure of 300 Atm was chosen to insure that the exit pressure is still supercritical. Supercritical pressure is necessary at the outlet of the core because the fuel wall temperature is far above the critical temperature of water.

The coolant velocity was limited by considerations of the mechanical integrity of the fuel plates (see Par. IV below).

1) "rendement" = ratio of maximum flux in the external reflector to total power.
Considering corrosion problems, a lower velocity was also deemed desirable, although for these particular operating conditions it seems likely that the corrosion is not strongly velocity dependent (see Par. VII below). A velocity of 32 m/sec was chosen.

3. Cladding

Preliminary results showed that clad temperatures would be too high for aluminium. Inconel 625 was chosen as the cladding material, because of its good combination of superior high temperature creep properties, and resistance to corrosion (see Par. VII below).

4. Fuel

Stainless steel 16-13 \(^2\) was chosen as the matrix material for the fuel element meat. A stainless steel matrix is appropriate for a high fuel loading, which is desired to increase the length of the operating cycle. The chosen material is softer than the Inconel cladding, which is an advantage for fabrication reasons.

The fuel is in the form of \(\text{UO}_2\) (40 wt % of the meat), with uranium enrichment to 93\(\%\) \(^{235}\text{U}\).

5. Overall Assembly and Dimensions

A thin core ring was necessary for several reasons. First, the fuel utilization is better for a thin core, since there is a smaller interior region of low flux. Secondly, the "critical velocity" of the radially-mounted fuel plates is greater for a lower plate width (see Par. IV below).

A central region of beryllium leads also to better fuel utilization, since there is a region of high flux at the core inner boundary also. A central region is at any rate necessary for fabrication reasons.

The height of the core is limited by several factors. The regions at the top and bottom of the core do not contribute as much as the central region to the maximum flux in the reflector. Also, the pressure drop and maximum clad temperature increase with height.

\(^2\) Composition given in Par. III below.
The final overall dimensions chosen were as follows:

- Inner Core Radius, \( R_i \) : 7.0 cm
- Outer Core Radius, \( R_o \) : 13.5 cm
- Core Height, \( H \) : 100 cm
- Core Volume, \( V \) : 42 liters

The fuel element dimensions were restricted to 0.5 mm "meat" and 0.15 mm cladding by thermal stress considerations (see Par. VI below). The coolant channel width was 1.2 mm; a smaller width would have caused an excessive pressure drop.

6. Pressure Barrel

The pressure barrel, which was located between the core and the \( D_2O \)-reflector, was made of an aluminium alloy (\( Zn Mg Cu 0.5 \)) of 1.4 cm thickness. The choice of this material was based on reactivity considerations. Diffusion theory calculations (discussed in Par. III) showed that a 1 cm stainless steel barrel reduced \( k_{\text{eff}} \) by about 20\%. The aluminium barrel caused a reduction of only about 3.5 \% in \( k_{\text{eff}} \).

Zircalloy was rejected because of excessive thermal stresses caused by the \( \gamma \)-heating and its poor thermal conductivity.

With the aluminium alloy chosen, however, the temperature increase in the barrel wall produced by the assumed \( \gamma \)-heating of 160 W/gr was only 60\(^\circ\)C. Thus, due to the very good mechanical properties of this alloy, the stresses caused by temperature and water pressure were sufficiently below the yield point.

III. Nuclear Calculations

1. Introduction

Most of the nuclear calculations were made with the Karlsruhe Nuclear Program System NUSYS \( [7] \). This system contains various programs which allow the generation of group cross sections and the calculation of reactor parameters with one- and two-dimension diffusion theory. The cross section values used were the "ABN" data of ABAGIAN et al. \( [7] \), which is a 26-group set. To investigate the problem of the thermal cross sections, which are strongly spatially dependent in the core, several calculations were also made with THERMOS \( [8] \), a thermalization transport theory program.
2. Material Regions and Cross Sections

The core composition was described in Par. II above. The supercritical H₂O-coolant had an average density of 0.97 g/cc. The approximate weight concentrations of the most important elements in Inconel 625 and stainless steel 16/13 are given in Table 1:

<table>
<thead>
<tr>
<th>Material</th>
<th>Density at room temp.</th>
<th>Cr</th>
<th>Ni</th>
<th>Fe</th>
<th>Mo</th>
<th>Nb</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inconel 625</td>
<td>8.44 g/cc</td>
<td>22.9</td>
<td>61.2</td>
<td>9.1</td>
<td>4.2</td>
<td></td>
</tr>
<tr>
<td>Stainless steel 16/13</td>
<td>8.0 g/cc</td>
<td>16.5</td>
<td>13.5</td>
<td>65.1</td>
<td>1.0</td>
<td></td>
</tr>
</tbody>
</table>

It was presumed that the UO₂-SS powder was compacted to 96% of its theoretical density, based on the data given in (11) for a similar case (SS 316, 35% UO₂). The density of the clad and meat have small corrections for their elevated temperatures. The values obtained for the compositions of the various mixtures are given in Table 2.

<table>
<thead>
<tr>
<th>Mixture</th>
<th>Material</th>
<th>Density (atom/cc, 10⁻²⁴)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 (Trap)</td>
<td>Be</td>
<td>1.22 · 10⁻¹</td>
</tr>
<tr>
<td>2 (Core - 100% Fuel)</td>
<td>Cr, Fe, Ni, Mo, Nb, U₂O₅</td>
<td>5.8 · 10⁻³, 8.8 · 10⁻³, 2.33 · 10⁻², 4.2 · 10⁻⁴, 9.6 · 10⁻³, 1.74 · 10⁻³</td>
</tr>
<tr>
<td>3 (Tank)</td>
<td>Al</td>
<td>6.0 · 10⁻²</td>
</tr>
<tr>
<td>4 (Reflector)</td>
<td>D, O</td>
<td>6.64 · 10⁻², 3.32 · 10⁻²</td>
</tr>
<tr>
<td>5 (H₂O above core)</td>
<td>H, O</td>
<td>6.50 · 10⁻², 3.25 · 10⁻²</td>
</tr>
</tbody>
</table>

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3) DIN 17 006 Nomenclature: X8 Cr Ni Mo V Nb 1613.
The "100% Fuel" of mixture 2 refers to the mixture with the normal UO$_2$ concentration. For use for flux-flattening in the boundary regions, core mixtures with UO$_2$ concentrations of 70%, 50%, 35%, and 25% of that of mixture 2 were also calculated. The stainless steel 16/13 concentration in these mixtures was raised correspondingly.

3. Methods and Results of NUSYS Calculations

A two-dimensional calculation with the full 26-energy groups of the ABN-set would require a prohibitive amount of time. Hence a one-dimensional diffusion theory calculation was first made for an approximately equivalent spherical shell reactor. The "equivalent spherical shell" had the same core thickness as for the final cylindrical two-dimensional calculation; the inner radius of the equivalent spherical shell core was determined by requiring that the central sphere has the same chord length as that of the cylindrical "trap" in the two-dimensional reactor. The spectra calculated for the equivalent sphere were then used to condense the group constants from 26 to 4 energy groups as follows:

<table>
<thead>
<tr>
<th>ABN (26-Group) Set</th>
<th>Reduced (4-Group) Set</th>
<th>Energy</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 - 10</td>
<td>1</td>
<td>10.5 MeV - 21.5 keV</td>
</tr>
<tr>
<td>11 - 18</td>
<td>2</td>
<td>21.5 keV - 46.5 eV</td>
</tr>
<tr>
<td>19 - 25</td>
<td>3</td>
<td>46.5 eV - 0.215 eV</td>
</tr>
<tr>
<td>26</td>
<td>4</td>
<td>0.0252 eV</td>
</tr>
</tbody>
</table>

The thermal group constants for both sets are those of a room-temperature Maxwellian. These values were used, since the thermal spectrum is extremely spatially dependent, so that a condensation of the two or three lower ABN groups gives quite varied results for the core reduced set constants, depending on where in the core the condensing spectrum is taken from. Since the spectrum in the core middle is much "hotter" than this Maxwellian, these cross sections are quite rough. The effect of this fact is investigated in Par. III.4, below. Figures 1 and 2 show typical spectra for condensation in the core and reflector, respectively.

4) The equivalence was in the sense that the calculated spectra should be appropriate for condensation. $k_{\text{eff}}$ was quite different from that of the two-dimensional reactor.
Using the condensed cross sections, the final calculations were made using the two-dimensional diffusion theory code DIXY. These calculations were repeated for various configurations and combinations of boundary zones, with the following requirements:

1) core ring thickness \( \approx 6.5 \text{ cm} \),
2) core height \( \approx 100 \text{ cm} \),

until the following conditions were satisfied:

3) \( k_{\text{eff}} \approx 1.21 \),
4) \( F_{\text{rad}} \approx 1.7 \),

where \( F_{\text{rad}} \) is the radial source density shape factor.

The reasons for desiring a limit on the core ring thickness and height, which led to 1) and 2), have been mentioned in Par. II.5.

Condition 3) was imposed to give sufficient excess reactivity for the following factors:

<table>
<thead>
<tr>
<th>Factor</th>
<th>Excess Reactivity</th>
</tr>
</thead>
<tbody>
<tr>
<td>a) Beam tubes</td>
<td>5%</td>
</tr>
<tr>
<td>b) Xe and Sm</td>
<td>3%</td>
</tr>
<tr>
<td>c) Stabile fission products</td>
<td>6.4%</td>
</tr>
<tr>
<td>d) Uranium burnup</td>
<td>3%</td>
</tr>
<tr>
<td>e) Control</td>
<td>1%</td>
</tr>
<tr>
<td>f) Temperature</td>
<td>0.5%</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>18.9%</strong></td>
</tr>
</tbody>
</table>

The total of 18.9% excess reactivity corresponds to a value for \( k_{\text{eff}} \) of about 1.21. The figures are based on calculations for the Franco-German High Flux Reactor, FGHFR. a), c), e), and f) are about the same values as for the FGHFR. b) is lower than the value of 5% for the FGHFR, since for the reactor discussed here only about half the fissions are in the thermal group. d) is lower because of an average burn-up lower than for the FGHFR. All figures are estimated and somewhat high in order to be conservative.

For condition 4), the maximum value of \( F_{\text{rad}} \) was somewhat arbitrarily set at 1.7, to keep the power peaks at the core boundary within acceptable limits. This required that there be a number of core boundary regions with reduced fuel concentration.
Figure 3 shows the final configuration for the reactor, and the resulting dimensions and nuclear characteristics are contained in Table 5. The rendement has been corrected to the approximate value expected for $k_{\text{eff}} = 1.0$, based on calculations for the FGFR. At the core-reflector interface there are four boundary zones, all 2.5 mm wide, with 25%, 35%, 50%, and 70% of the normal fuel concentration. At the "trap"-core interface one 2.5 mm zone with 70% fuel concentration was used.

Figure 4 shows the radial power distribution at the reactor midplane. Due to the fact that the thermal group constants used are for a Maxwellian, this distribution is very rough. However, as THERMOS calculations discussed in Par. III.4, below indicate, these peaks are probably overestimated, so that the results are conservative.

Based on the figures given in Table 5, a maximum thermal flux in the reflector of $10^{16}/\text{cm}^2\text{sec}$ would be reached with a total power of 500 MW. This corresponds to an average power density of 11.9 MW/\ell, and a maximum power density of 25.6 MW/\ell.

4. Thermal Group Cross Sections

As mentioned above, the assumption of Maxwellian values for the fourth energy group is not accurate, since the thermal spectrum in the core becomes increasingly "hardened" as the distance from the boundary is increased. Thus the thermal cross sections become increasingly smaller as the interior of the core is approached.

To investigate the influence of this effect on $k_{\text{eff}}$, several diffusion theory calculations were made in which the thermal cross sections were reduced to half their original value. All of the thermal cross sections were changed, since all of the important cross sections have approximately a $1/\nu$-dependence and would be equally changed by a spectral shift. Hence $k_{\text{eff}}$ is relatively insensitive to this change; $k_{\text{eff}}$ never changed more than 1% due to this variation. The change in the rendement was not more than 3%.

The most serious error caused by presuming constant Maxwellian cross section values is that the spatial distribution of the thermal fissions is not correctly calculated. The leveling of the fission distribution is an important problem in a reactor with a high uranium concentration. Therefore, the problem of the fission "peaks" was investigated using the program
THERMOS, which calculates the spatially dependent thermal neutron spectrum using transport theory. Two one-dimensional slabs were calculated which corresponded to UO$_2$-concentrations of "100%" (Mixture 2, Table 2) and "25%". The slab consisted of a three cm "core" region, and a 15 cm reflector region. The hydrogen and deuterium scattering in the H$_2$O and D$_2$O was represented by Nelkin kernels, and the oxygen scattering was represented by a free gas with a mass of 16. All other core materials were grouped into one fictional material with the appropriate constant total scattering cross section. The core absorption cross section was that for the "100%" or "25%" UO$_2$-concentration, with a 1/v-dependence. At the two outer slab boundaries reflector boundary conditions were assumed, since these boundaries corresponded approximately to the middle of the core and maximum reflector thermal flux position in the NUSYS calculation. The results from a NUSYS calculation were used for the epithermal flux distribution, needed to calculate the source distribution in THERMOS. This flux distribution was practically constant.

Figure 5 shows how strongly the core thermal spectrum varies from the reflector interface to the middle of the core, for the 100% UO$_2$-concentration. Particularly for the core middle, presuming that the thermal group stops at 0.215 eV is very inaccurate. The average energy of the neutrons below 0.215 eV varies from 1.62 kT at a point in the reflector 15 cm from the core, to 3.47 kT in the core middle. Hence the energy dependent cross sections averaged over the spectrum up to 0.215 eV, i.e.

$$\int_0^{0.215 \text{ eV}} \phi(E,x) \sigma(E) \, dE / \int_0^{0.215 \text{ eV}} \phi(E,x) \, dE$$

are quite spatially dependent.

However, for our calculations what interests one is the spatial dependence of the thermal fissions, which describes the "peaks" in the power density. This dependence is the same as that of

$$\int_0^{0.215 \text{ eV}} \phi(E,x) \sigma_a(E) \, dE$$

from THERMOS, since the fission and absorption cross sections both have 1/v-dependence in the thermal energy range.
To compare with the THERMOS values, NUSYS calculations were made with the same geometry as for the THERMOS cases, but using the cross sections as for the NUSYS reactor calculations. Figures 6 and 7 compare the NUSYS and THERMOS results for the spatial dependence of the fissions caused by neutrons below 0.215 eV. As can be seen, in both cases the NUSYS calculations overestimate the thermal fission peaks. Thus it is probable that the fission peaks in the NUSYS reactor calculations are also overestimated, and these results are conservative. Also, since non-thermal fissions make a large contribution to the total fission distribution in the NUSYS calculations, the total fission peaks are not as much in error as are the thermal fission peaks.

IV. Cooling-Water Critical Velocity

The critical velocity is the velocity at which the modifications of local velocity resulting from a deflection of the plates produce across the plates a pressure difference sufficient to maintain the deflection.

To calculate the critical velocity it was assumed that the evolvent fuel element plates can be approximated by a circumference arc (see Figure 8). According to reference [13] the critical velocity of a flat plate with built-in edges with a width equal to the chord of the curved plate considered is given by:

\[ v_c = \sqrt{\frac{15\ E\ a^3\ s_a}{\rho_a\ b^4\ (1 - \nu^2)}} \] (1)

where:  
- \( E \) = Young's modulus of Inconel 625 at 508°C = \( 1.785 \times 10^{12} \) dynes/cm²
- \( \nu \) = Poisson's ratio of Inconel 625 at 508°C = 0.319
- \( a \) = plate thickness = 0.08 cm
- \( b \) = chord of the curved plate = 8.8 cm
- \( s_a \) = nominal thickness of flow channel = 0.12 cm
- \( \rho_a \) = mean coolant density = 0.97 gr/cm³

The resulting \( v_c \) is 5.61 m/sec. A considerable extrapolation of the graphs of reference [13] shows that a curved plate, with \( \frac{b}{a} = 110 \) and an arc of 1.42 rad., such as that considered in Figure 8, has a critical velocity about 18 times higher than the above value, i.e., about 100 m/sec.
This value was obtained with two simplifying assumptions:
a) The evolvent is approximated by a circumference arc. This is a pessimistic assumption because the evolvent under an uniform pressure difference deforms in a way that the plate rotates about a point in the middle which is not displaced \( \sqrt{14} \); thus the effective plate width is about half of the real value.
b) The plate has been presumed homogeneous and formed by Inconel 625 at 508°C, which is an optimistic assumption because the Young's modulus of the metal is lower \( (1.492 \times 10^{12} \text{ dynes/cm}^2) \) than that of Inconel 625.

If the geometrical tolerances of the coolant channel and of the plates were zero, one could theoretically work with a water velocity of 100 m/sec; in practice, the small dimensional differences between adjacent channels have a strong influence on the stresses on the fuel plates. To take into account this effect, a rather high safety factor was assumed. The selected velocity was 32 m/sec, which is equivalent to a safety factor of 3.

V. Heat Transfer Calculations

The pressure drop in the average channel is given by:

\[
\Delta p (\text{Atm}) = 0.9869 \times 10^{-2} \cdot \rho_a v_a^2 \left( 0.25 + \frac{\lambda_a H}{4s_a^2} \right)
\]

where

\( \rho_a = \text{mean water density} = 0.97 \text{ gr/cm}^3 \)
\( v_a = \text{mean water velocity} = 32 \text{ m/sec} \)
\( 0.25 \) gives the pressure loss and recovery at channel inlet and outlet
\( H = \text{channel length} = 100 \text{ cm} \)
\( s_a = \text{channel nominal thickness} = 0.12 \text{ cm} \)
\( \lambda_a = \text{friction coefficient given by the Moody correlation} \left( \frac{15}{12} \right) \)
\( = 0.0239, \) for a Reynolds number equal to \( 4.24 \times 10^5 \) and a dimensionless roughness of the wall equal to 0.002. To take into account the temperature effect the physical properties of water appearing in the Reynolds number have been evaluated at the average fuel element wall temperature.

The resulting \( \Delta p \) is 51 Atm. This means a force acting on the core in the vertical direction of about 14 tonnes.
The heat transfer calculation has been performed for the hot streak 5). To take into account local variations of roughness the friction coefficient has been assumed to be 10% higher than the average. Due to geometrical tolerances it has also been assumed that the average thickness of the cooling channel corresponding to the hot streak is 0.02 cm less than the nominal value. The resulting water velocity at the hot streak is therefore:

\[ v \approx v_a \sqrt{\frac{\lambda_{a} s}{\lambda_{s} s_{a}}} = 32 \sqrt{\frac{1}{1.1} \cdot \frac{0.1}{0.12}} = 27.3 \text{ m/sec} \quad (3) \]

In the core the fuel element surface is equal to \(10^4 \text{ cm}^2\) per liter, so that a maximum power density of 25.6 MW/l is equivalent to a maximum heat flux of 2560 W/cm\(^2\) = 612 cal/cm\(^2\) sec. For \(F_{ax} = 1.27\) the average hot streak heat flux is \(q_m = 482\text{ cal/cm}^2\text{ sec}\) and the outlet water temperature is given by:

\[ T_3 = T_1 + \frac{2 q_m H}{c_{pm} \beta_m v_s} = 40 + \frac{2 \cdot 482 \cdot 100}{1.288 \cdot 0.895 \cdot 2.78 \cdot 10^3 \cdot 0.1} = 342^\circ\text{C} \quad (4) \]

The hot streak water temperature in the middle of the core is:

\[ T_2 = T_1 + \frac{q_m H}{c_{pm} \beta_m v_s} = 40 + \frac{482 \cdot 100}{1.026 \cdot 0.895 \cdot 2.78 \cdot 10^3 \cdot 0.1} = 229^\circ\text{C} \quad (5) \]

These calculations must be performed by iteration because the specific heat is rather strongly temperature dependent. The product \(\rho C\) has been assumed constant; in reality it varies like \(V^{1/2}\), because \(V^2\) is constant, but \(\rho V^{1/2}\) varies little in the range of temperatures and pressures considered.

The fuel plate wall temperature has been calculated with the Colburn correlation with an uncertainty factor equal to 0.35 \(\sqrt{167}\). The heat transfer coefficients in the middle and at the outlet of the hot streak are respectively:

\[ \text{The hot streak is the line in the vertical direction at the point in the radial direction where the axially integrated power has a maximum.} \]
\[ h_2 = 0.85 \cdot 0.023 \frac{k_2}{2s_a} \left\{ \frac{2y^m}{\nu_2} \right\}^{0.3} \text{Pr}_2^{0.33} = \]
\[ = 0.85 \cdot 0.023 \frac{1.539 \cdot 10^{-3}}{2 \cdot 0.12} \left( \frac{2 \cdot 0.895 \cdot 2.78 \cdot 10^3 \cdot 0.1}{1.243 \cdot 10^{-3}} \right)^{0.3} 0.8435^{0.33} \]
\[ = 3.70 \text{ cal/cm}^2 \text{sec}^0\text{C} \]  

(6)

\[ h_3 = 0.85 \cdot 0.023 \frac{1.167 \cdot 10^{-3}}{2 \cdot 0.12} \left( \frac{2 \cdot 0.895 \cdot 2.78 \cdot 10^3 \cdot 0.1}{0.785 \cdot 10^{-3}} \right)^{0.3} 1.043^{0.33} \]
\[ = 4.22 \text{ cal/cm}^2 \text{sec}^0\text{C} \]  

(7)

And the wall temperatures are given by:

\[ T_{W2} = T_2 + \frac{q_{\text{max}}}{h_2} = 229 + \frac{612}{3.70} = 394^0\text{C} \]  

(8)

\[ T_{W3} = T_3 + \frac{q_3}{h_3} = 342 + \frac{344}{4.22} = 424^0\text{C} \]  

(9)

The wall temperature varies very little in the second half of the core and it reaches a maximum of 424°C at the core outlet corresponding the hot streak.

These high temperatures require the use of supercritical water; the water must be sufficiently supercritical at the hot streak outlet to avoid a pseudo-burnout. The pressure of 249 atm is considered sufficiently above the critical pressure (225 atm).

The maximum temperatures inside the fuel plates are obviously in the middle of the core on the hot streak. The temperature at the interface cermet-Inconel is given by:

\[ T_{I2} = T_{W2} + \frac{q_{\text{max}} s_2}{k_{\text{Inc}}} = 394 + \frac{612 \cdot 0.015}{0.0403} = 622^0\text{C} \]  

(10)

where \(s_2\) = thickness of the Inconel cladding
\nk_{\text{Inc}} = \text{thermal conductivity of Inconel at } 508^0\text{C} .

The maximum cermet temperature is given by:

\[ T_{C2} = T_{I2} + \frac{q_{\text{max}} s_1}{2k_{\text{cermet}}} = 622 + \frac{612 \cdot 0.025}{2 \cdot 0.031} = 869^0\text{C} \]  

(11)
where

\[ s_1 \text{ is equal to half the thickness of the cermét} \]

the factor of 2 is given by the parabolic temperature distribution in the cermét

\[ k_{\text{cermét}} \text{ is the cermét thermal conductivity.} \]

In the previous calculation it has been assumed that no temperature difference exists at the interface between cermét and cladding.

VI. Thermal Stresses

The following assumptions were made:

a) The temperature distribution in a horizontal cross section of the fuel plates is symmetric with respect to the central plane; only strains in the axial direction have been considered.

b) The temperature distribution in the cermét is parabolic, while in the cladding it is linear.

c) The effect of the built-in edges is neglected.

Then the thermal stresses in the cermét and in the cladding are given respectively by:

\[ \sigma_1(x) = \frac{K E_1}{1-\nu_1} - \frac{E_1}{1-\nu_1} \alpha_1 \left[ \Delta T_c - \left( \Delta T_c - \frac{\Delta T_1}{s_1} \right) \frac{x^2}{s_1^2} \right] \]  \hspace{1cm} (12)

\[ \sigma_2(x) = \frac{K E_2}{1-\nu_2} - \frac{E_2}{1-\nu_2} \alpha_2 \left[ \Delta T_1 - \frac{\Delta T_1 - \Delta T_2}{s_2} (x - s_1) \right] \]  \hspace{1cm} (13)

where \( K \) is the strain in the axial direction, common to cermét and cladding, and is given by:

\[ K = \frac{E_1 \alpha_1}{1-\nu_1} \frac{s_1}{3} (2 \Delta T_c + \Delta T_1) + \frac{E_2 \alpha_2}{1-\nu_2} \frac{\Delta T_1 + \Delta T_2}{2} s_2 \]  \hspace{1cm} (14)

The most dangerous thermal stresses are those corresponding to the maximum fuel plate temperatures, that is for:

\[ \sigma_1(x) \]
\[ \Delta T_c = \text{difference between maximum cermet temperature and room temperature} = 869 - 20 = 849^\circ C \]

\[ \Delta T_I = \text{difference between maximum cermet-cladding interface temperature and room temperature} = 622 - 20 = 602^\circ C \]

\[ \Delta T_W = \text{difference between fuel plate wall temperature at the middle of the hot streak and room temperature} = 394 - 20 = 374^\circ C \]

The physical properties of cermet and Inconel involved are the following:

\[ E_1 = \text{Young's modulus of the cermet at } 737^\circ C = 0.4 E_{uo2} + 0.3 E_{s.s.16/13} = 0.4 \times 17.45 \times 10^5 + 0.6 \times 13.7 \times 10^5 = 15.2 \times 10^5 \text{ kg/cm}^2 \]

\[ \alpha_1 = \text{thermal expansion coefficient of cermet between } 787^\circ C \text{ and } 20^\circ C = \text{thermal expansion coefficient of stainless steel } 16/13 \text{ between } 787^\circ C \text{ and } 20^\circ C = 18.99 \times 10^{-6} \text{ }^\circ C^{-1} \]

\[ \nu_1 = \text{Poisson's ratio of cermet at } 737^\circ C = 0.4 \nu_{uo2} + 0.6 \nu_{s.s.16/13} = 0.4 \times 0.301 + 0.6 \times 0.33 = 0.3184 \]

\[ E_2 = \text{Young's modulus of Inconel at } 505^\circ C = 18.2 \times 10^5 \text{ kg/cm}^2 \]

\[ \alpha_2 = \text{thermal expansion coefficient of Inconel between } 505^\circ C \text{ and } 20^\circ C = 14.0 \times 10^{-6} \text{ }^\circ C^{-1} \]

\[ \nu_2 = \text{Poisson's ratio of Inconel at } 505^\circ C = 0.319 \]

With these numerical values and \( s_1 = 0.025 \text{ cm}, s_2 = 0.015 \text{ cm}, \) equations (12,13) become:

\[ \sigma_1(x) = 1.674 \cdot 10^7 x^2 - 1.073 \cdot 10^4 \text{ (kg/cm}^2) \quad (15) \]

\[ \sigma_2(x) = 0.770 \cdot 10^4 + 5.687 \cdot 10^5 (x-0.025) \text{ (kg/cm}^2) \quad (16) \]

\( \sigma_1(x) \) is always negative (compression) and has a maximum in the center of the cermet equal to -10,750 kg/cm². At the interface cermet-Inconel \( \sigma_1(x) \) is equal to -270 kg/cm², \( \sigma_2(x) \) is always positive (tension). At the interface cermet-Inconel \( \sigma_2(x) = 7700 \text{ kg/cm}^2 \) at the interface Inconel-water \( \sigma_2(x) \) has a maximum equal to 16,230 kg/cm². The yield strength of Inconel 625 at the interface cermet-cladding (622°C) and cladding-water (394°C) are respectively 5090 kg/cm² and 5180 kg/cm². One can see then that the thermal stresses in Inconel, which should be the material giving strength to the plates, are very high. Thermal stresses about twice the yield strength are normally accepted, in our case, however, they are three times the yield strength.
Furthermore, the Inconel is subjected to the pressure of the cermot, which, being not able to sustain the axial compression caused by thermal stresses, tends to expand in the transversal direction. However, these transversal stresses in Inconel should be very small due to the very small rigidity of the cladding in the transversal direction.

Obviously, to reach definite conclusions, it would be necessary to confirm these calculations and assumptions by means of experiments. However, it appears that, in a first approximation, with these plates the factor limiting the temperatures, and, therefore, the heat fluxes is the thermal stress.

VII. Corrosion

As mentioned in Par. II, one of the reasons for choosing Inconel 625 for the cladding material is its great resistance to corrosion. The supercritical water used for the cooling in this investigation represents an extreme condition for the cladding material, and makes corrosion a very important problem. At cladding temperatures in the range needed for this reactor \(T_{\text{max}} \approx 530^\circ\text{C}\) the normal stainless steels are very susceptible to stress-corrosion cracking \(\text{[17,18]}\). Nickel alloys and alloys of the Incoloy type show better resistance to corrosion in \(\text{H}_2\text{O}\) at high temperatures \(\text{[19]}\). Two alloys in particular, Inconel 625 and Incoloy 800, have been tested rather extensively at General Electric to determine their corrosion characteristics in high-temperature \(\text{H}_2\text{O}\text{[19-21]}\). For the test conditions, Inconel 625 showed corrosion resistance somewhat better than that of Incoloy 800 in the temperature range of interest here (about 530\(^\circ\text{C}\)). Furthermore, Inconel 625 has a greater strength and better creep properties at high temperatures than Incoloy 800 \(\text{[19,22]}\).

Even with the data available for Inconel 625, the amount of corrosion of the fuel plates is extremely hard to estimate. Corrosion is a very complex phenomenon, and there are a number of parameters involved in a corrosion test, such as clad temperature, \(\text{H}_2\text{O}\)-velocity, \(\text{H}_2\text{O}\)-density, \(\text{H}_2\text{O}\)-temperature, and the heat transfer rate through the cladding. The tests made at General Electric \(\text{[19-21]}\) are for clad temperatures and velocities comparable to those of the reactor considered here, but for a much lower heat transfer rate and \(\text{H}_2\text{O}\)-density. Table 3 compares the conditions in the reactor considered here with those for one element of the G.E. test loop.
Since the corrosion is such a complex process, it is extremely difficult to extrapolate from the values obtained for the test loop conditions to determine those values which would be expected for the reactor conditions. However, LEISTIKOW has indicated that it would probably be a fair approximation to presume that, for the case considered here, the corrosion depends only on the clad surface temperature. This assumption is based on the fact that already in the conditions of the G.E. test loop, the oxide layer on Inconel 625 was extremely thin compared to that of most other metals, and was practically non-existent on much of the clad-surface. Hence, greater heat transfer and \( H_2O \)-density, which might in many cases contribute to a faster removal of oxide and associated faster corrosion, should not be expected to have a strong influence for the particular case under consideration here. If this assumption is correct, then one can see from Table 3 that the corrosion is negligible for the short cycle time of the reactor core (≈ 6 days, see Par. VIII).

Another factor which could influence corrosion is the effect of radiation with fast neutrons, which was of course not present in the G.E. test loop. Unfortunately, there are no investigations of this effect known to us.
At any rate, the value for corrosion from Table 3, $2.5 \cdot 10^{-4}$ mm is about 600 times less than the clad thickness (0.15 mm). Hence, the corrosion could be many times greater than the value in Table 3, and still be within acceptable limits.

VIII. Core Lifetime

No burn-up calculations were made for this investigation. However, an estimate of the lifetime of the core was made using the following assumptions, and applying a correction based on burn-up calculations for the Franco-German High Flux Reactor:

a) The limiting point for burn-up is the point of maximum flux, on the core-reflector boundary.

b) During the core lifetime, the flux at the core-reflector boundary does not change.

c) For the uranium concentration at the boundary ($\approx 170$ g/l) the maximum allowable burn-up is 70%.

The following comment should be made concerning b). This assumption is, of course, not entirely correct, due to the change of the uranium spatial distribution during the period of operation. However, this assumption should give reasonable accuracy in the results, since at this point the thermal flux, which causes the major part of the burn-up at the boundary, comes primarily from "in-streaming" from the reflector. This in-streaming should not vary too much during the period of operation, for a constant maximum thermal flux in the reflector.

To calculate the allowable burn-up time the following expression was used:

$$U(t) = U(0) e^{-\sum_i \sigma_a^i \phi^i t}$$

where

- $U(t) = ^{235}U$-concentration at core-reflector interface
- $t = \text{time after start of cycle}$
- $\sigma_a^i = \text{absorption cross section of } i\text{-th group of 4-group set}$
- $\phi^i = \text{flux in } i\text{-th group, at core-reflector interface}$. 
Using assumption c), \( U(t_m) / U(0) = 0.3 \), one gets \( \sum_i \sigma_a^i \phi^i t_m = 1.2 \), where \( t_m \) is the time for maximum burn-up. Using the values from Table 4, one arrives at the result that \( t_m = 5.6 \) days.

Table 4  Core nuclear values at core outer radius (\( \phi^i \) values based on maximum \( \phi^\text{th} \) in reflector of \( 10^{16}/\text{cm}^2\text{sec} \))

<table>
<thead>
<tr>
<th>( i )</th>
<th>( \sigma_a^i ) (barn)</th>
<th>( \phi^i ) (cm(^{-2}) sec(^{-1}))</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.7</td>
<td>( 1.1 \times 10^{16} )</td>
</tr>
<tr>
<td>2</td>
<td>14.5</td>
<td>( 2.82 \times 10^{15} )</td>
</tr>
<tr>
<td>3</td>
<td>55.0</td>
<td>( 2.1 \times 10^{15} )</td>
</tr>
<tr>
<td>4</td>
<td>603.</td>
<td>( 3.8 \times 10^{15} )</td>
</tr>
</tbody>
</table>

This calculation was checked by using the same method to calculate the cycle time for two cases considered for the Franco-German High Flux Reactor and comparing the results with those of accurate burn-up calculations which existed for the same cases. The accurate burn-up calculations were performed using the two-dimensional diffusion code TURBO/24/. For both cases the results using expression (17) were about 4.5% lower than those from the TURBO calculations. Hence the corrected core lifetime for the case considered here is about \((1.045)(5.6) \approx 6 \) days.

**IX. Fuel Cycle Costs**

For the fuel elements envisaged — i.e. \( \text{UO}_2 \) in stainless steel matrix with Inconel clad — no statements on the reprocessing and refabrication costs can presently be made. But in order to get at least a rough idea of the annual fuel cycle costs of the reactor, two cases were considered:

a) Optimistic - The fabrication and reprocessing costs are presumed to be the same as those for a core with aluminium as the "meat" matrix and fuel plate cladding ("aluminium core").

b) More realistic - The fabrication costs are 3 times, and the reprocessing costs are 1.5 times those of an aluminium core.
The yearly fuel cost \( "K" \) is related to the cost per core, \( K_{\text{core}} \), through:

\[
K = \frac{365}{T+2} \cdot K_{\text{core}} \tag{18}
\]

where \( T = \) cycle time for one core in days.

Two days are allowed for changing the core. With \( T = 6 \) (see Par. VIII),

\[
K \approx 45 K_{\text{core}}
\]

Because of large uncertainties in the cost approximation, a greatly simplified equation can be used to calculate \( K_{\text{core}} \):

\[
K_{\text{core}} = K_{\text{fabr}} + K_{\text{repr}} + \beta K_u \tag{19}
\]

where

- \( K_{\text{fabr}} = \) fabrication costs
- \( K_{\text{repr}} = \) reprocessing costs
- \( K_u = \) uranium cost for a core
- \( \beta = \) average burn-up = 0.12.

The core being considered contains 25 kg of \( ^{235}\text{U} \). With a uranium price of 43.2 DM/g \( ^{235}\text{U} \), \( \beta K_u = 0.13 \cdot 10^6 \text{ DM} \).

Case a)

The reprocessing costs are about \((1-\beta)\cdot1600 \text{ DM/kg}\) for an aluminium core \( ^{237}\).

Then

\[
K_{\text{repr}} = 0.40 \cdot 10^6 \text{ DM} ,
\]

furthermore

\[
K_{\text{fabr}} = 0.30 \cdot 10^6 \text{ DM} .
\]

Thus

\[
K_{\text{core}} = 0.83 \cdot 10^6 \text{ DM} ,
\]

and

\[
K = 37 \cdot 10^6 \text{ DM/yr} .
\]

Case b)

For this case:

\[
K_{\text{repr}} = 0.606 \cdot 10^6 \text{ DM} ,
\]

and

\[
K_{\text{fabr}} = 0.900 \cdot 10^6 \text{ DM} .
\]

Then

\[
K_{\text{core}} = 1.63 \cdot 10^6 \text{ DM} .
\]

Thus the cost for the uranium used is less than 10\% of the total fuel cycle cost. The yearly fuel cost is \( K = 73 \cdot 10^6 \text{ DM/yr} \).
Use of a more accurate formula which also takes into account the fuel interest rates and the dependence of the reprocessing costs on the enrichment would increase both total costs by about 10%.

X. Conclusions

A summary of the important characteristics of the reactor is given in Table 5. One can see that it is possible to obtain a maximum thermal flux in the reflector of $10^{16}/\text{cm}^2\text{sec}$ with a total power of 500 MW.

However, this relatively low total power implies a very high power density, which in turn leads to high fuel element temperatures. Therefore, Inconel 625 was chosen as the cladding material. Due to the low thermal conductivity of this alloy, the limiting factor for the power density is the magnitude of the thermal stresses. The thermal conductivity of aluminium is much higher. However, even if we assume that the geometrical tolerances of the fuel plates are zero, the maximum temperature in the aluminium plates is too high (about 370°C) to permit the use of this metal.

On the other hand, if one wants a neutron flux in the reflector of $0.5\cdot10^{16}$ n/cm² sec only, then this is probably possible with aluminium (maximum hot spot temperature estimated at less than 300°C). The fuel cycle costs would be about four times smaller, due to smaller fabrication costs. It is evident that the step from $0.5\cdot10^{16}$ n/cm² sec to $1\cdot10^{16}$ n/cm² sec, which requires the use of Inconel 625 in place of aluminium as cladding material, is achieved with a considerable decrease (2 times) in the ratio neutron flux to fuel cycle costs.

The very short operating cycle and the large fraction of costs which goes into core fabrication leads one to consider other types of reactors, such as a homogeneous reactor. However, in our opinion the technical problems associated with homogeneous reactors are even more complicated than the type we considered in this report. Due to the extremely high costs and experimental inconvenience associated with such a short cycle, more detailed investigations were not made. Many further problems were not considered, such as:

1) radiation damage and sealing of the pressure barrel,
2) radiation damage and heat dissipation in the beam tubes,
3) thermal stresses in the fuel plates due to temperature gradients in the radial direction,
4) heat removal from a cold source,
5) support of a core with an inlet-outlet pressure difference of 50 Atm.
Table 5  Preliminary design characteristics of a 10^16-flux beam research reactor

<table>
<thead>
<tr>
<th>1. Dimensions and composition</th>
<th>2. Operating characteristics</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>D_2O-reflector:</strong></td>
<td><strong>Coolant velocity</strong> 32 m/sec</td>
</tr>
<tr>
<td>diameter</td>
<td>300 cm</td>
</tr>
<tr>
<td>height</td>
<td>200 cm</td>
</tr>
<tr>
<td>pressure</td>
<td>near atmospheric</td>
</tr>
<tr>
<td><strong>H_2O-core:</strong></td>
<td><strong>Coolant temperature at core entry</strong> 40°C</td>
</tr>
<tr>
<td>diameter</td>
<td>27 cm</td>
</tr>
<tr>
<td>height</td>
<td>100 cm</td>
</tr>
<tr>
<td>central flux trap filled with Be, diameter</td>
<td>14 cm</td>
</tr>
<tr>
<td>core volume</td>
<td>42 l</td>
</tr>
<tr>
<td><strong>Al-pressure barrel:</strong></td>
<td><strong>Mean coolant temperature at core exit</strong> 160°C</td>
</tr>
<tr>
<td>between core and reflector, thickness</td>
<td>1.4 cm</td>
</tr>
<tr>
<td><strong>Fuel element:</strong></td>
<td><strong>Maximum coolant temperature at core exit</strong> 342°C</td>
</tr>
<tr>
<td>one ring HFIR-type with radial power flattening</td>
<td></td>
</tr>
<tr>
<td><strong>meat:</strong></td>
<td><strong>Pressure at core entry</strong> 300 Atm.</td>
</tr>
<tr>
<td>stainless steel 16/13 with 40% fully enriched UO_2</td>
<td></td>
</tr>
<tr>
<td>clad:</td>
<td><strong>Pressure at core exit</strong> 250 Atm.</td>
</tr>
<tr>
<td>INCONEL 625</td>
<td>0.15 mm</td>
</tr>
<tr>
<td>coolant channel</td>
<td>1.2 mm</td>
</tr>
<tr>
<td>average fuel load</td>
<td>686.235 U/l</td>
</tr>
<tr>
<td><strong>Coolant:</strong></td>
<td><strong>Maximum heat flux</strong> 2560 W/cm^2</td>
</tr>
<tr>
<td>supercritical water</td>
<td><strong>Average heat flux</strong> 1190 W/cm^2</td>
</tr>
<tr>
<td></td>
<td><strong>Maximum wall temperature of fuel plate</strong> 530°C</td>
</tr>
<tr>
<td></td>
<td><strong>Maximum power density</strong> 25.6 MW/l</td>
</tr>
<tr>
<td></td>
<td><strong>Average power density</strong> 11.9 MW/l</td>
</tr>
<tr>
<td></td>
<td><strong>Total power</strong> 500 MW</td>
</tr>
<tr>
<td></td>
<td><strong>Maximum unperturbed flux in the reflector</strong> 1.0 \cdot 10^{16} /cm^2 sec</td>
</tr>
<tr>
<td></td>
<td><strong>k_{eff} of fresh core</strong> 1.21</td>
</tr>
<tr>
<td></td>
<td><strong>Length of cycle</strong> 6 d</td>
</tr>
<tr>
<td></td>
<td><strong>Average burn-up</strong> 12%</td>
</tr>
</tbody>
</table>
Appendix A: Isotope Reactor

An "isotope reactor", i.e., one which has the maximum flux in the central trap, was also calculated. The core composition and the overall dimensions were the same as for the beam reactor (except that the outer radius of the reflector was only 45 cm for this case).

For the isotope reactor, the central trap was filled with H₂O, and the reflector was Be. There was no pressure barrel for this case. Furthermore, the core boundary zones were switched so that there were 2.5 mm zones of 25%, 35%, 50%, and 70% fuel concentration on the inner core boundary, and 50% and 70% on the outer boundary. The factors such as $F_{ax}$, $F_{rad}$, $k_{eff}$, etc. were practically the same as for the beam reactor. The thermodynamic characteristics were presumed to be essentially the same as those of the beam reactor. For the same total power as the beam reactor, i.e., 500 MW, the maximum thermal flux in the trap center was about $2 \cdot 10^{16}$. No calculations were made for the case of a loaded trap.
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Fig. 1. Spectrum from 26-group diffusion theory calculation used for condensation of group constants of mixture 2, Table 2 (core-100% fuel) from 26- to 4-groups (see Par. III.3.).

Lethargy, \( u = \ln \left( \frac{E^*}{E} \right) \) where \( E^* = 10.5 \text{ MeV} \).
Fig. 2. Spectrum from 26-group diffusion theory calculation used for condensation of group constants of mixture 4, Table 2 (D₂O-reflector) from 26- to 4-groups (see Par. III.3.).

Lethargy, \( u = \ln \left( \frac{E}{E^*} \right) \) where \( E^* = 10.5 \text{ MeV} \).
Fig. 3. Quarter cross-section of configuration for final beam reactor calculation. The numbers are those of the mixtures in Table 2. 2a, 2b, 2c, and 2d are the core mixtures with UO$_2$-concentrations of 70%, 50%, 35%, and 25% of that of mixture 2, respectively.
Fig. 4. Radial power distribution at beam reactor midplane, for final 4-group diffusion theory calculation.

(--- ---) Only one point, at $r = 7$ cm, calculated in this region. At boundary points between various core zones, power values were not calculated; curves were extrapolated graphically to these points.
Fig. 5. Results for core thermal spectra from THERMOS (see Par. III.4.).

- 3 cm from core-reflector interface (approximately middle of core)
- In core at core-reflector interface
- Maxwellian distribution
Fig. 6. Thermal fission distribution as function of distance, x, from core-reflector interface, for UO₂-concentration that of mixture 2, Table 2 (see Par. III.4.).

--- 4-group diffusion theory calculation; ϕ σₜ of 4th (thermal) group

--- THERMOS calculation; ∫₀₉₀ ₂₁ ₅ eV σₐ(E) ϕ(x,E) dE
Fig. 7. Thermal fission distribution as function of distance, x, from core-reflector interface, for \( UO_2 \)-concentration 25% that of mixture 2, Table 2 (see Par. III.4.).

--- 4-group diffusion theory calculation; 
\[ \phi \sigma_f \text{ of 4th (thermal) group} \]

--- THERMOS calculation; 
\[ \int_0^{0.215 \text{ eV}} \sigma_a(E) \phi(x,E) \, dE \]
$R_i = \text{inner radius}$

$R_o = \text{outer radius}$

--- evolvent fuel plate

--- circumference arc by which the evolvent is approximated in the calculation of the critical velocity

**Fig. 3.** Transversal cross-section of the core