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of the Air Cycle Nuclear Reactor

To: A. M. Weinberg

From: J. A. Lane

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Forward

The Air Cycle Reactor Analysis Committee was established to assess the magnitude of certain unsolved problems involved in the design of the air cycle nuclear reactor as proposed by the General Electric Company ANP Group.

This committee is composed of the following ORNL personnel:

J. A. Lane, Long Range Planning Group, Chairman

L. C. Noderer, Long Range Planning Group, Nuclear
Calculations

E. R. Mann, ANP Division, Control

W. B. Harrison, Reactor Experimental Engineering Division,
Heat Transfer

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Individual discussions with numerous other members of ORNL and with members of the ANP Project, General Electric Company have contributed significantly to the analysis effort.

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Summary

A brief study of the feasibility of the air cycle reactor based on the preliminary design as presented in Report DC-51-9-28, "Preliminary Design of an Air Cycle Reactor and Shield" by the Engineering Division, ANP Project, General Electric Company, Oak Ridge, September 26, 1951, has been carried out. An attempt has been made to verify the major assumptions made in the report, to define the limitations of the air cycle in general, and to point out major problems.

The results of this study indicate that while a reactor of the type proposed probably can be designed to meet the requirements of a low performance plane, the magnitude of certain difficulties inherent to the air cooling cycle may be considerably underestimated. For this reason, a short time schedule for the construction of this reactor does not seem reasonable.

The first serious problem which has not been adequately considered in the proposed design is the excessive loss of fast neutrons due to streaming in air channels. This may lead to a considerable increase in the amount of U-235 required for criticality above that estimated in the report. Unfortunately, the analytical approach to the problem is inaccurate and at best can only serve to compare various configurations. With the shelf design as proposed, the U-235 inventory may be greater than 300 pounds. Since a reflector tends to offset the effect of neutron streaming, the importance of having a completely reflected reactor should be stressed. In addition, the possibility exists of modifying the design of fuel channels to reduce the streaming problem. In this regard, cylindrical channels not more than two inches in diameter are preferable

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to the shelf design or concentric annuli.

A second problem which has not been adequately considered is the design of the nuclear control system. The low heat capacity of the fuel plates combined with the high power density in the reactor makes this type of reactor extremely sensitive to the behavior of the air and water moderator systems or to momentary power surges. The reliability and hence the feasibility of the air cycle reactor therefore depends on precise control of both water and air flow. This in turn must be coupled to a sensitive and rapidly responding nuclear control system. In addition the possibility of designing a mechanical control rod system that will not cause perturbations in the neutron flux pattern and thus increase local surface temperatures seems remote.

One problem inherent to an air cooled reactor of the type proposed is the difficulty of estimating the heat transfer requirements within the reactor with sufficient accuracy. Although the design seems adequate on the basis of heat transfer data used, it is possible to show that equally reliable heat transfer equations in the literature predict metal surface temperatures higher than the design limit. These temperatures, moreover, are sensitive to local changes in heat transfer coefficients, air distribution and fuel loading. While it would be desirable in such a system to incorporate a large safety factor in the amount of available heat transfer area, the size limitations of the reactor make this impossible. For this reason, an extensive research and development effort may be necessary to provide heat transfer data accurate enough to insure satisfactory performance of the reactor.

It is felt that in general the principle of a water moderated, heterogeneous, solid fuel reactor will lead to a highly desirable type of reactor

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for aircraft propulsion. Since most of the difficulties of the proposed design are due to the use of air as a coolant, similar designs using alternate coolants such as fused salts should be considered.

The report which follows is divided into four parts, prepared by members of the Oak Ridge National Laboratory staff. These are:

- I. Nuclear Characteristics
- II. Control
- III. Heat Transfer Considerations
- IV. Shielding

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Nuclear Characteristics of Air Cycle Reactor

The basic method of approach to the design of the reactor core proposed in Report DC-51-9-28 using a one group model seems reasonable enough as a first approximation. In this method the heat transfer area and thus the amount of stainless steel in the reactor is first established on the basis of the power generation requirements. An amount of water necessary for a reasonable moderating efficiency is then added to establish the size of the reactor and thus the buckling constant. This is corrected for the effect of the reflector. This permits an estimate of the amount of U-235 necessary for criticality with a reflected, homogeneous mixture of the water, insulator, structural materials and fuel. This inventory must then be corrected for additional neutron losses due to the following effects:

- a) non-uniform loading of fuel to achieve flat radial power
- b) increased absorption in water due to heterogeneous lattice
- c) streaming of neutrons out of fuel channels
- d) Xenon, Samarium and other fission product poisons
- e) fuel depletion during flight.

Thus the validity of this method of establishing the nuclear characteristics of the reactor depends largely on the accuracy of the correction for the effects listed above. These are considered separately as follows:

- a) Non-uniform loading of fuel for flat radial power distribution.

The method of determining the fuel distribution in order to completely flatten the radial power distribution is discussed by Megzer.⁽¹⁾ A perturbation type treatment is used, which assumes that for monoenergetic neutrons in a bare reactor, the scattering cross section and flux in space

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distribution are not changed significantly. The validity of this assumption was verified by inserting the requirements for flat power in the two-group differential equations for a homogeneous cylindrical reactor with an infinite water reflector. (An infinite water reflector (three inches or greater thickness) may be substituted for the six inch beryllium described in the report to simplify calculations. It is believed that such a reflector moreover may be equally effective.) A comparison of the thermal neutron flux for flat power loading with the flux for uniform loading is shown in Figure 1. This indicates that the non uniform distribution of U-235 in the reactor has only a small effect on the thermal flux distribution. The non-uniform loading will require 1.05 - 1.10 times as much fissionable material as the uniformly loading reactor.

An alternate method of achieving uniform radial power distribution and at the same time decreasing the amount of U-235 required is described by Goertzel.⁽²⁾ In this method, the distribution of fuel and moderator is adjusted to give a uniform radial thermal neutron flux. This requires putting enough fuel in the center of the reactor so that the absorption of thermal neutrons by the fuel just balances the production in the moderator. This effect is enhanced if the amount of moderator is at the same time decreased toward the center of the reactor. A consideration of the possibility of flattening the radial flux in the proposed air cycle reactor indicates that the lattice structure and dimensions will have to be altered to do this. It is conceivable that in this manner a uniform radial power distribution can be achieved without appreciably increasing the uranium inventory.

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b) Increased absorption in water due to heterogeneous lattice.

Cell Correction

We have not attempted to verify the cell correction, which depends on the relative distribution of thermal neutrons in the moderator and metallic fuel element since this factor is so susceptible to the particular cell design chosen. Measurements in an appropriate critical experiment would seem to be the only reliable basis for such a calculation. Furthermore, the cell correction has a lesser effect on the critical mass calculations than streaming or pile flattening. The value of the ratio of neutron flux in the moderator to that in the fuel recently suggested by the GE-ANP group of 1.85 has been used in our calculations.

c) Streaming of neutrons out of fuel channels.

The loss of neutrons by direct streaming out of fuel channels and the additional leakage due to increased diffusion lengths in voids, constitutes the greatest uncertainty in the estimation of the uranium inventory required for criticality in the proposed reactor. This effect has been largely ignored in the uranium requirements shown in the preliminary design report. Consequently an attempt to estimate neutron losses due to the presence of air channels in the reactor is presented herewith.

The first effect to be considered is the direct streaming of virgin neutrons out of the fuel channels in which they are produced. Here we will consider only neutrons which escape on first flight without being scattered by the iron which is assumed to be distributed uniformly throughout the channel. The fraction of neutrons which escape from the flat rectangular channels may be estimated by analogy to equivalent flat disks of the same composition, assuming uniform production of neutrons

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throughout. The escape from the edge of thin disks is shown in Figure 2 which is applicable as long as the radius of the disk is appreciably greater than the thickness. For the shelf design, with channels 4.1 cm wide and assuming that σ_s for stainless steel = 3 barns ($N \sigma_s = .023$), the fractional escape of fission neutrons will be about 6 percent. Correcting this for the cosine distribution in the direction of air flow reduces this to about a 5 percent loss of virgin neutrons. This effectively reduces η from 2.12 to about 2.01.

For the tubular type of construction, an estimate of the fractional escape from the ends of cylinders is required. This is shown in Figure 3 which has been derived by graphical integration methods. Each tube is assumed to be 91.5 cm long and contain the same amount of stainless steel per unit volume as in the shelf design. A cosine distribution for the production of neutrons in a tube has been included. It is obvious from Figure 3 that for tubes with radius less than 10 cm, the escape of virgin neutrons will be less in the tubular design than in the shelf design.

A second factor introduced by the presence of air channels in the pile is the additional leakage of both fast and thermal neutrons due to the effect of voids on the diffusion lengths. This has been considered by Weinberg and Fermi,⁽³⁾ for cylindrical ducts and more recently by Behrens,⁽⁴⁾ for ducts of various cross sections. According to Behrens, the presence of voids effectively increases the diffusion lengths by the factor,

$$1 + 2\phi + \frac{2r\phi}{\lambda (e^{2r/\phi} - 1)} + \frac{Qr\phi}{\lambda}$$

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where,

ϕ = ratio of void volume to material volume (for proposed reactor $\phi \approx 1.78$)

r = hydraulic radius of void (= 5.0 cm)

λ = mean free path of neutrons in pile ($\lambda_{fast} = 3.6$ cm)

Q = mean square passage length through the hole $\div$ the square of the mean length

Values of Q for ducts of various cross sections are reported by Behrens.⁽⁴⁾ No estimate of Q for rectangular shaped ducts is given. Consequently Behrens' method has been extended by Noderer to include ducts or fuel channels of this cross section. Values of Q for various rectangular configurations are plotted in Figure 4 as a function of the ratio of the width to the thickness of the channel. For the proposed shelf design the average Q is about 2.2. For concentric annuli Q is approximately equal to 2.0.

If one assumes that the stainless steel and uranium are mixed in with the water and that the fuel channels and insulator are voids*, the age $\bar{\tau}$ of the homogeneous pile will be increased by the factor given above divided by $(1 + \phi)^2$. Thus

$$\frac{\tau}{\bar{\tau}} = \frac{1 + 2\phi + \frac{2r\phi}{\lambda(e^{2r/\phi} - 1)} + \frac{2r\phi}{\lambda}}{(1 + \phi)^2}$$

Taking,

$$\phi = 1.78, \quad r = 5.0 \text{ cm}, \quad \lambda = 3.6 \text{ cm}, \quad Q = 2.2$$

$$\bar{\tau} = \text{homogeneous age} = 207 \text{ cm}^2$$

$$\tau = \frac{11.32}{7.73} = 1.46 (207) = 303 \text{ cm}^2$$

* This is a good approximation since bulk density of Refrasil is only 2-6% of crystal SiO_2 .

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For the diffusion of thermal neutrons the effective hydraulic radius of voids is taken as the width of the gap between individual fuel plates (~ 0.4 cm). On this basis, the L^2 of the homogeneous pile will be increased by a factor of 1.12 due to streaming. Thus $L^2 = 18(1.12) = 20 \text{ cm}^2$.

The above calculations over-emphasize the effect of neutron streaming, particularly in the case of fast neutrons. The scattering of fast neutrons by the stainless steel, SiO_2 and uranium, and in particular inelastic scattering will certainly make the channel act somewhat better than a true void. For this reason Behrens' method, or that of Weinberg and Fermi is not strictly applicable and an extension of the simplified treatment is necessary.

One method of estimating the effective age in two heterogeneous regions containing materials with different scattering and slowing down properties has been proposed by Noderer. In this method, the age for each region is first calculated separately to determine the ratio of the two. In other words,

$$\frac{\tau_0}{\tau_1} = \frac{\sum s_1 \sum \tau_1 \xi_1}{\sum s_0 \sum \tau_0 \xi_0} = \alpha^2$$

Likewise it is assumed that the effective age of the heterogeneous mixture will be inversely proportional to $\sum_s \sum_{Tr} \xi$, taking the proper average values for the scattering and transport cross sections and slowing down power. Since the scattering is more or less independent of angle of collision, the correct average scattering cross section is the same as for the average of a homogeneous mixture. The mean transport cross section, however, does depend on angular distribution and therefore on the respective geometries of the two regions. Since Behrens has already treated the case of diffusion through piles with voids, it is possible to apply his method to estimate the proper mean cross sections as follows:

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First, the material in one region is divided into two fractions designated by single and double primes and the first of these fractions adjusted so that its age is equal to that for the second region. In other words,

$$\Sigma_{s1} \Sigma_{Tr1} \xi_1 = \Sigma_{s0}'' \Sigma_{Tr0}'' \xi_0$$

and

$$\Sigma_{s0}'' = \alpha \Sigma_{s0}$$

$$\Sigma_{Tr0}'' = \alpha \Sigma_{Tr0}$$

$$\xi'' = \xi_0$$

α^2 is given above.

What remains is the fraction $1 - \alpha$ of the material in the first region so divided and a void in the second region. The effective transport mean free path for this fraction may now be treated by Behren's method which considers the diffusion of neutrons through a region containing a void.

The transport mean free path for this fraction of the material in the reactor should be corrected as follows:

The factor,

$$\left[1 + 2\phi + \frac{\phi^2 (2r/\phi \lambda')}{(e^{2r/\phi \lambda'} - 1)} + \frac{Qr\phi}{\lambda'} \right] = X$$

given by Behrens corrects mean square lengths for the presence of voids.

where $\lambda' = \lambda / (1 - \alpha)$

Thus,

$$\lambda'_{Tr} = \frac{\lambda_{Tr0} X}{(1 - \alpha)(1 + \phi)}$$

$$\Sigma'_{Tr} = \frac{(1 + \phi)(1 - \alpha) \Sigma_{Tr0}}{X}$$

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and

$$\begin{aligned}\bar{\Sigma}_{Tr} &= \Sigma'_{Tr} + \Sigma''_{Tr} = \Sigma_{Tr_0} \left[\alpha + \frac{(1+\phi)(1-\alpha)}{X} \right] \\ \bar{\Sigma}_s &= \Sigma'_s + \Sigma''_s = \Sigma_{s_0} \left(\alpha + \frac{1-\alpha}{1+\phi} \right)\end{aligned}$$

The effective age relative to the age in the first region is therefore,

$$\frac{\tau}{\tau_0} = \frac{1}{\left[\alpha + \frac{(1-\alpha)}{1+\phi} \right] \left[\alpha + \frac{(1+\phi)(1-\alpha)}{X} \right]}$$

This formula is now the general case of fast neutron diffusion in a reactor containing regions of different scattering properties. Behrens' solution occurs when $\tau_1 = \infty$ or $\alpha = 0$. Obviously as the behavior of the material in the channel approaches that of the moderator, τ/τ_0 approaches 1.

To evaluate the effect of streaming alone, or in other words, the correction to the age calculated for a homogeneous mixture of the reactor materials, the above expression should be divided by $\left[\alpha + \frac{1-\alpha}{1+\phi} \right]^2$. The effective age is now given by,

$$\frac{\tau}{\bar{\tau}} = \frac{\left[\alpha + \frac{1-\alpha}{1+\phi} \right]}{\left[\alpha + \frac{(1+\phi)(1-\alpha)}{X} \right]}$$

where, $\bar{\tau}$ = age of homogeneous mixture.

Applying this formula to the conditions specified in the proposed reactor, where $\bar{\tau} = 207 \text{ cm}^2$, $\tau_0 = 46 \text{ cm}^2$ (for Al + H₂O mixture)

$\tau_1 = 30000 \text{ cm}^2$ (for S.S., SiO₂, U mixture) $\alpha = .04$, $\lambda' = 3.75 \text{ cm}$,

$r = 5.0 \text{ cm}$

$$\frac{\tau}{\bar{\tau}} = \frac{0.385}{0.279} = 1.38$$

And

$$\tau = 286 \text{ cm}^2$$

It should be mentioned that the above formula is based on some rather broad assumptions about the scattering characteristics of the reactor and in particular, neglects the contribution of inelastic scattering to the value of ξ_1 for stainless steel. Since the streaming correction is fairly sensitive to the value taken for the age in the mixture of SiO_2 , stainless steel and uranium, which can not be estimated with any degree of accuracy, the calculation at best should be considered with some reservation.

If one assumes that the age calculated for the homogeneous mixture is correct (207 cm^2) then the estimate of the streaming contribution, obtained by assuming the channels are voids, gives an upper limit of the effective age (303 cm^2). Likewise if one assumes that the stainless steel in the channel acts like the H_2O moderator itself (in which case $\alpha = 0.2$) then a lower limit of the effective age of 248 cm^2 is obtained.

It should be noted that in addition to providing an estimate of the effect of streaming on the age of the fast neutrons in a heterogeneous lattice, the above method permits a calculation of the age of the corresponding homogeneous mixture. Thus,

$$\frac{\bar{\tau}}{\tau_0} = \frac{1}{\left(\alpha + \frac{1 - \alpha}{1 + \phi}\right)^2}$$

where, $\tau_0 = \text{age of } \text{H}_2\text{O at } .034 \text{ ev} = 36 \text{ cm}^2$

τ_1 for the mixture of Al, SiO_2 and steel = 3800 cm^2

$\alpha = 0.1$

$\phi = 2.31$

$\tau = 260 \text{ cm}^2$ for a homogeneous mixture

This compares with the value of 207 cm^2 reported for the homogeneous mixture and on this basis the age, including the effect of streaming, will be about 360 cm^2 . This is probably an overestimate of the effective age. First of all, the method is not strictly applicable when the fraction of

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heavy elements becomes large compared to the hydrogen. Second, the effect of fast fissions has been neglected which by reducing the logarithmic energy interval between neutron production and absorption decreases the age. Opposing this, however, there is a preferential absorption by the steel in the resonance region compared to uranium.

d) Xenon, Samarium and other fission product poisons.

The Xenon absorption cross section one-hour after shutdown of the reactor is given by,

$$\Sigma_{Xe} = \frac{.0337}{\frac{0.627}{\Sigma_f} + 2.05} + .0012$$

The absorption cross section of samarium and other permanent fission product poisons has been studied by D. T. Bray, Reference 5. Assuming 100 hours operation of the reactor, the value of $\frac{\Sigma_{poison}}{\Sigma_{fission}}$ is given, for various fluxes as follows:

Thermal Flux	$\frac{\Sigma_{poison}}{\Sigma_{fission}}$
2.5×10^{13}	.005
5×10^{13}	.007
10^{14}	.009

e) Fuel depletion during flight.

The U-235 burn-up amounts to about .05 grams per MW per hour of operation. After 100 hours, the total burn-up will only be about 750 grams of U-235. This must be added to the initial inventory calculations.

Uranium Inventory

On the basis of the above considerations it is evident that the uranium inventory will be considerably increased due to the streaming of fast neutrons, but relatively unaffected by the loss of thermal neutrons. A recalculation of the amount of U-235 necessary for criticality, one hour

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after shutdown, after operating at full power is presented as follows:

$$\eta f - (1 + L^2 \kappa^2) e^{\kappa^2 \tau} = 0$$

where,

η = neutrons produced per absorption in uranium - neutrons lost by streaming of virgin neutrons = 2.01

$$f = \frac{\Sigma_a(u)}{\Sigma_a(u) + \Sigma_a(\text{Xe} + \text{Sm}) + \Sigma_a(\text{S.S.} + \text{SiO}_2 + \text{Al}) + d \Sigma_a(\text{H}_2\text{O})}$$

κ^2 = equivalent reflected buckling constant

$$= .001714$$

$$L^2 = \text{migration area} = 20 \left(\frac{\Sigma_a(\text{total})}{\Sigma_a(\text{total}) + \Delta \Sigma_a(u)} \right) \text{ cm}^2$$

$$\tau = \text{age} = 285 \text{ cm}^2 \text{ (probable value with streaming)}$$

d = correction for increased absorption in H_2O due to heterogeneous cell = 1.85.

Since both L^2 and $\Sigma_a(\text{Xe} + \text{Sm})$ depend on the amount of U-235 in the reactor, the critical equation given above must be solved by trial and error. The results of such a calculation give an uranium inventory of about 310 pounds uncorrected for flat power loading. For uniform loading, the following data are valid.

$\Sigma_a(u)$	$= 0.1040 \text{ cm}^{-1}$
$\Sigma_a(\text{Xe} - 1 \text{ hr.})$	$= 0.0050 \text{ cm}^{-1}$
$\Sigma_a(\text{Sm} + \text{F.P.})$	$= 0.0005 \text{ cm}^{-1}$
$\Sigma_a(\text{S.S.} + \text{SiO}_2 + \text{Al})$	$= 0.00943 \text{ cm}^{-1}$
$\Sigma_a(\text{H}_2\text{O}) \times 1.85$	$= 0.00750 \text{ cm}^{-1}$
$\Sigma_a(\text{total})$	$= 0.1254 \text{ cm}^{-1}$
f	$= 0.8301$
L^2	$= 11 \text{ cm}^2$
$(1 + \kappa^2 L^2)$	$= 1.019$
$e^{\tau \kappa^2}$	$= 1.631$

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This means that the uranium inventory is more than doubled due to the streaming of fast neutrons.

While it may be that the fast neutron streaming calculations presented above are not valid due to the presence of stainless steel and uranium in the fuel channels, the estimated increase in uranium required is significant enough to warrant careful consideration of this effect. The only accurate practical approach is a Monte Carlo type calculation with a given reactor configuration. A properly designed critical experiment, however, should give some indication of the amount of fast neutron leakage. In the case considered above, the leakage of fast neutrons increases from 30 percent to 39 percent due to additional diffusion through voids plus an additional 5 percent due to direct loss of virgin neutrons out of fuel channels.

The problem of reducing the loss of fast neutrons by streaming is essentially one of designing fuel channels so that no more than one dimension is appreciably greater than the mean free path of neutrons in the pile. This means that with cylindrical fuel channels not more than 1-1/2 in. to 2 in. in diameter the streaming will be relatively unimportant. Large concentric annuli, on the other hand, will not appreciably reduce the amount of streaming.

Control

Since no specific method of control of either the overall power plant or the reactor core has been worked out for the preliminary design of the air cycle reactor, only a generalized consideration of the control problems for this reactor can be presented. The evolution of a specific control element design may very well turn out to be a complex process until a basic philosophy for controlling this power plant is decided upon. Such a decision should be made before too many design features are frozen, since the incorporation of control elements will certainly have an important bearing on the structural design and heat removal considerations.

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Certain specific control problems may be cited which certainly will require a considerable development effort for their complete solution. Some of these have been touched upon in the report, while others of equal significance have either not been considered as control problems or if they have been considered, have not been given sufficient importance in determining a realistic completion deadline.

The first question concerns the requirements for stabilizing the reactor against transient changes in activity. In this respect the inherent stability imparted to the reactor by the water moderator must be considered. Since the thermal base is below the peak of the xenon 135 absorption curve, it is unlikely that the reactor will have a positive reactivity temperature coefficient. The effectiveness of negative reactivity temperature coefficient of the water, however, is considerably reduced by the fact that only a small fraction of the heat is generated in the water and the insulator prevents heat from being conducted to the water. In other words temperature rise of the water under a given power surge is far less than the corresponding temperature rise of the fuel plates. Since the ratio of the temperature rise in the water and the temperature rise in the fuel plates is proportional to the heat generated in the water and fuel and to their respective total heat capacities, it is highly desirable to keep the moderator cooling system as small as possible. This means separate water systems for the shield and moderator. Under such conditions, the temperature of the fuel plates will increase about 130 times as fast as the moderator temperature for a given power surge. Thus the moderator would rise about 5 °F before the hottest fuel plate melted. This is equivalent to a change in $\frac{\Delta k}{k}$ of about .0005 which is the maximum self-stabilizing effect to be expected from the water with no control rods operating. With a mixed shield and moderator water system, the inherent stability imparted by the water is negligible.

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It is therefore evident that even under the best conditions, reactivity changes greater than 0.5 percent must be controlled or they will result in excessive fuel element temperatures. This means also that fluctuations in the temperature of the moderator itself must be held to less than 5 °F for satisfactory performance.

The possibility of introducing very short periods during start up of the reactor places a considerable demand on the performance of the regulating control system. Two characteristics of this type of reactor magnify this problem. First, the neutron life-time is fairly short ($< 10^{-4}$ secs) and second, there is a considerable excess reactivity built into the reactor to permit xenon override after shutdown. With start-up times of the order of 5 minutes, (which means changing k at the rate of 1 percent/minute) periods as low as 0.1 sec could theoretically result. Such periods, unchecked could cause excessive fuel plate temperatures 1/3 sec after reaching the design power level. Under such conditions, the insertion of neutron absorbing ions into the moderator stream would not be fast enough for satisfactory control. What is required is a rapidly responding, mechanical type, regulating rod capable of operating under a wide variety of load conditions.

One can not estimate with any degree of reliability, the power response to moderator transients nor can one estimate the power response to large reactivity changes brought about by control rod motion until solutions to the kinetic equations of the reactor have been obtained for some of these conditions. For this reactor, these equations are a set of non-linear differential equations for which no analytical solution is known. Responses can be calculated by use of digital computers and may in the near future be obtained by use of a simulator or analogue type of computer.

The interaction between the turbojets and the reactor constitutes one of the most difficult phases of the control problems. Determination of a

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satisfactory schedule for shifting from reactor power to chemical power and vice versa can not be considered a trivial engineering problem. For one thing, fuel injection into the hot air stream may not be a simple thing to do. Interaction between engines, where four turbojets are in parallel, in case of a stall and the disastrous effect of such a stall must be considered a possibility. A stall of more than one engine simultaneously would in all likelihood wreck the reactor if it is operating at design power. There is an unfortunate combination of adverse control features here. There is a tight coupling between engines, tight coupling between engines and reactor and conditions for instability in both the reactor and engines. The control problems are not those for isolated units such as an engine or a reactor but of a single system.

Although provision is made for scrambling the reactor in response to certain emergency signals, in an aircraft power plant relying on nuclear energy alone such a scram would be fatal. With a combination chemical and nuclear power plant, scrambling the reactor requires an immediate chemical fuel substitution. This means a complicated system of rapidly responding valves etc., all tied into the control system.

One problem which should not be overlooked which is inherent to all aircraft reactors is that very little of the nuclear instrumentation has been tested at design point altitude and under conditions of vibration, tilts and intermittent accelerations which may prevail during flight. In short, an extensive testing program may be necessary to develop instruments suitable as airborne control equipment.

With regard to the effect of control elements on heat transfer considerations, the possibility exists that any mechanical rod system will cause local variations in the thermal neutron flux pattern and disturb the flat power distribution. In many reactors, the cooling of the control rod itself and

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its effect on the flow of coolant in adjacent regions becomes a heat transfer problem which must be integrated with the entire system.

To summarize the control problem, the inherent master-slave relationship which would allow the reactor to provide whatever power at whatever temperature the engines requested is not a feature of the design of the air cycle reactor. Consequently the range in which partial failure of the power plant with its associated complex and interlocking control equipment still permits operation at reduced power is extremely limited. The extreme demand placed on the performance of the regulating control system certainly means an extensive design and development effort.

Heat Transfer Considerations

A. Air Cooling System

The use of air as a reactor coolant, while simplifying the external power plant system, places a considerable demand on the ingenuity of those who must design the reactor core. In other words for the given size of the reactor core and the amount of power which must be extracted, it is not possible to incorporate much of a safety factor into the design. For this reason, the possibility of exceeding the maximum specified metal surface temperature at full power due to local variations in flow, heat transfer coefficients or non uniform loading must be examined in detail. The following questions become pertinent:

- a) How much non-uniformity in air distribution can be tolerated?
 - b) How close can heat transfer coefficients be specified?
 - c) What will be the effect of non-uniform radial power generation?
 - d) What is the magnitude of local hot spots?
- a) The answer to the first question has been considered by W. B. Harrison⁽⁶⁾ as follows:

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At the entrance to the reactor, the effect of variation in air distribution in the reactor is shown primarily in the change of heat transfer coefficient with stream velocity and the resulting change in temperature difference between the heat exchange surface and the air stream. At the outlet end of the reactor, the effect is shown primarily in the change in air stream temperature. In order to illustrate these statements, the example by Tribus and Krase⁽⁷⁾ may be referred to.

The conditions in the example are as follows:

T_o , inlet air temperature 606 °F

T_f , outlet air temperature 1500 °R

T_{w1} , wall temperature at end of first fuel element 1650 °F

T_{w2} , wall temperature at beginning of last fuel element 1650 °F

T_1 , air temperature at end of first fuel element 659 °F

T_2 , air temperature at beginning of last fuel element 1446 °F

Consider a decrease in mass velocity to eighty percent of the design value.

Thus the temperature change of the air is $\frac{1}{0.8}$ or 1.25 times the design value.

At the end of the first fuel element, the stream temperature will become

$$T'_1 = 606 + \frac{659 - 606}{0.8} = 672 \text{ °F} \quad (T_1 = 659 \text{ °F})$$

The value of $T_w - T$ will be increased by the factor $\left(\frac{1}{0.8}\right) = 1.195$, according to Equation 9 of Reference (7).

Hence,

$$T'_w - T'_1 = 1.195 (1650 - 659) = 1184 \text{ °F} \quad (T_{w1} - T_1 = 991 \text{ °F})$$

The required wall temperature becomes

$$T'_{w1} = 1184 + 672 = 1856 \text{ °F} \quad (T_{w1} = 1650 \text{ °F})$$

Similarly, at the beginning of the last increment,

$$T'_2 = 606 + \frac{1446 - 606}{0.8} = 1656 \text{ } ^\circ\text{F}$$

$$(T_2 = 1446 \text{ } ^\circ\text{F})$$

$$T'_{w2} - T'_2 = 1.195 (1650 - 1446) = 244 \text{ } ^\circ\text{F}$$

$$(T_{w2} - T_2 = 204 \text{ } ^\circ\text{F})$$

The required wall temperature becomes

$$T'_{w2} = 1656 + 244 = 1900 \text{ } ^\circ\text{F}$$

$$(T_{w2} = 1650)$$

Thus, at the entrance, the change of heat transfer coefficient accounts for 193 degrees in a total wall temperature change of 260 $^\circ\text{F}$. At the exit of the reactor, the change in stream temperature accounts for 210 degrees in a total wall temperature change of 250 $^\circ\text{F}$. This serves to emphasize once again the importance of carefully selecting design values for the heat transfer coefficients, particularly in the region of high temperature difference.

The considerations noted above are applicable regardless of the cause of variation in air distribution. Poor header design, local pressure variations, or partial plugging of a flow channel might lead to such a condition, resulting in increased wall temperatures.

b) With regard to methods of estimating values of heat transfer coefficients, four heat transfer equations reported in the literature have been compared with that given in Report DC-59-9-28 proposed by Tribus and Krase in Reference (6). Summarizing the results as follows we have:

$$(1) \quad h = 9.98 \times 10^{-4} \left(\frac{G^{0.8}}{D^{0.2}} \right) \quad (\text{Tribus and Krase})^{(7)}$$

$$(2) \quad h = 0.025 \mu^{0.2} C_p \left(\frac{G}{D} \right)^{0.8} \quad (\text{McAdams, Nicoli and Keenan})^{(8)}$$

$$(3) \quad h = 0.022 \frac{k}{\mu^{0.8}} \left(\frac{G}{D} \right)^{0.8} \quad (\text{Hoffman})^{(9)}$$

$$(4) \quad h = 0.023 \left(\frac{k}{\mu^{0.8}} \right) \left(\frac{C_p \mu}{k} \right)^{0.4} \left(\frac{G}{D} \right)^{0.8} \quad (\text{McAdams})^{(10)}$$

$$(5) \quad h = 0.019 \left(\frac{k_s}{\mu_s^{0.8}} \right) \left(\frac{\rho_s}{\rho} \right)^{0.8} \left(\frac{G}{D} \right)^{0.8} \quad (\text{Laudermilk and Grele, NACA})^{(11)}$$

Taking an arbitrary value of $h = 100$ from Equation (1) relative values of h for the other equations would be,

	<u>Air Temperature</u>	
	<u>600 °F</u>	<u>1500 °F</u>
(1)	100	100
(2)	72	86
(3)	93	107
(4)	83	97
(5)	53	86

The maximum deviation is 47 percent for 600 °F/air and 14 percent for 1500 °F air. Values estimated by other equations tend to be lower than that given by Tribus and Krase. The maximum deviation would result in approximately a 900 °F increase in surface temperature at the inlet end of the reactor. The average deviation might give about 150 °F excess temperature of the metal wall.

c) The effect of a change in fuel loading or heat generation may be shown in the same way as for air flow changes. Consider a channel in which heat is generated at a rate 20 percent greater than the design value. For this case, the air temperature change increases directly with the heat generation and the value of $T_w - T$ is dictated by the increased heat generation as in Equation 9 of Reference (7).

The design figures used in the previous examples may be used again in the present case. At the end of the first fuel element, the air temperature is

$$T_1' = 606 + 1.2 (659 - 606) = 670 \text{ °F} \quad (659 \text{ °F})$$

$$T_{w1}' - T_1' = 1.2 (1650 - 659) = 1189 \text{ °F} \quad (991 \text{ °F})$$

$$T_{w1}' = 1189 + 670 = 1859 \text{ °F} \quad (1650 \text{ °F})$$

At the beginning of the last fuel element,

$$T_2' = 606 + 1.2 (1446 - 606) = 1614 \text{ } ^\circ\text{F} \quad (1446 \text{ } ^\circ\text{F})$$

$$T_{w2}' - T_2' = 1.2 (1650 - 1446) = 245 \text{ } ^\circ\text{F} \quad (204 \text{ } ^\circ\text{F})$$

$$T_{w2}' = 1614 + 245 \text{ } ^\circ\text{F} = 1859 \text{ } ^\circ\text{F} \quad (1650 \text{ } ^\circ\text{F})$$

Thus, for the conditions in the example of Tribus and Krase, a 20 percent increase in heat generation increases the maximum surface temperature by slightly over 200 $^\circ\text{F}$.

d) The magnitude of possible "hot spots" has been considered by W. S. Farmer.⁽¹²⁾ His conclusion is that in the proposed fuel assembly composed of a honey comb of rectangular passages (Figure 5 of Report DC-51-9-28), the temperature at the hottest point is approximately 125 $^\circ\text{F}$ greater than the uniform fuel plate temperature.

The conclusion, after considering the answers to the above questions, is that at the design power level there is a good possibility of exceeding the maximum specified metal surface temperature of 1800 $^\circ\text{F}$ at some point and thereby reducing the reactor life.

B. Fuel Channel Insulator

The problem of preventing heat from being conducted from the hot fuel plates into the water moderator is not difficult. The most important consideration in this respect is the proper choice of insulator, from a nuclear as well as a heat transfer point of view. For the insulator specified in the preliminary design report, the heat conducted to the water is approximately,

$$\begin{aligned} q &= \frac{k}{l} A \Delta T \\ &= \left(\frac{2.4}{0.2}\right)(460) 1400 \\ &= 7.7 \times 10^6 \text{ Btu/hr} \\ &= 2100 \text{ Btu/sec} \end{aligned}$$

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The heat generated in the insulator by gamma absorption will amount to about 800 Btu/sec. The total heat into the water will be 2500 Btu per second or 1.6 percent of the total heat generated.

With an air gap of the same thickness instead of insulator between the stainless steel fuel plates and aluminum plates, the heat conducted to the water by convection and radiation would amount to about,

$$q = 0.18 A \Delta T^{1.25} + 2.4 \times 10^{-5} A \left[\left(\frac{T_1}{100} \right)^4 - \left(\frac{T_2}{100} \right)^4 \right]$$
$$= 200 + 2200 \text{ Btu/sec}$$

This means that a 50 mil air gap would probably be sufficient to prevent excessive heat loss by conduction to the water. This would add considerably to the nuclear characteristics of the reactor since the space formerly occupied by insulator could be replaced with water, with a resulting decrease in age and reduction in streaming.

C. Water Moderator System Cooling Requirements

The water moderator receives heat from gamma rays generated and absorbed within the core, from the neutron energy dissipated during the slowing down process and by direct conduction and radiation from fuel channels. The distribution of gamma and neutron heat generation is estimated as follows:

The gamma heat generated per fission is:

- 5 Mev of fission γ 's (each 1 Mev)
- 6.3 Mev of fission product γ 's (each 2.5 Mev)
- 0.25 Mev of H_2O capture γ 's (each 2 Mev)
- 3.15 Mev of Fe, U + Al capture γ 's (each 6 Mev)

The fraction which escape the pile and are absorbed in the water shield is:

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	<u>escape from sides</u>	<u>escape from ends</u>	<u>Total Mev/fission escape</u>
fission γ 's	10%	8%	0.9 Mev
fission product γ 's	15%	14%	1.8
H ₂ O capture γ 's	14%	12%	0.1
Fe, U, Al capture γ 's	24%	21%	<u>1.4</u>
			4.2 Mev

This means that 4.2 Mev of 2.2% of the total heat escapes the pile as gamma energy and is absorbed in the reflector and shield. The remaining 10.5 Mev of gamma heat which comprises 5.5% of the total is absorbed within the pile. This is distributed as follows in the various portions of the pile structure.

H ₂ O Moderator	1.9% of heat
Al	1.2%
SiO ₂ insulator	0.1%
stainless steel	2.3%

The heat from the neutrons is mainly from the slowing down of fast neutrons. The fast leakage is about 0.6 neutrons or 1.2 Mev per fission which is moderated and absorbed in the reflector and shield. The remainder of neutron heat or 3.8 Mev will be generated in the water moderator. The beryllium reflector capture gammas amount to less than 0.1% of the total heat and may be neglected. The generated heat distribution is summarized as follows:

	<u>gamma heat</u>	<u>neutron heat</u>	<u>total heat</u>
reflector + shield	2.2%	0.6%	2.8%
H ₂ O moderator	1.9	2.0	3.9
Aluminum structure	1.2	-	1.2
insulator	0.1	-	0.1
fuel plates	2.3	-	(92.0 by difference)

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The heat which must be removed in the water system is the sum of the heat generated in the reflector, moderator and aluminum which amounts to 7.9% plus that conducted through the insulator to the water. This latter contribution will amount to about 1.6% for the shelf design as shown previously. Thus the heat into the water is about 9.5% of the total compared to a value of 6% given in the preliminary design report. The heat removed by the water system, therefore, will amount to about 14500 Btu/sec which requires roughly 14000 sq. ft. of heat transfer surface in the radiator.

The problem of removing the heat from the water in the moderator or reflector and shield, while not particularly difficult at low airplane speeds can become acute at high velocities. This is because the stagnation temperature of the cooling air approaches that of the water and the Δt available for heat removal decreases. Consequently the ultimate performance of the water moderated reactor is limited by the temperature of the water, which in turn is limited by the pressure which the structure can be designed to accommodate.

The problem of maintaining a uniform temperature in the water moderator has an important bearing on the nuclear behavior of the reactor. This means that the water must be heated when the nuclear power plant is inoperative during flight. Under certain conditions the maintenance of a uniform water temperature could be troublesome.

Shielding

The problem of shielding the air cycle nuclear power plant differs from that for the liquid cooled reactors in two respects. First, the use of air as the cooling medium eliminates the need for shielding the external cooling lines due to induced activities in the coolant. This is a considerable advantage. The presence of air channels within the reactor,

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however, as well as the inlet and exit ducts which penetrate the shield increase the difficulty of shielding the reactor. As long as the required power plant performance is low, in which case the size of the inlet and exit ducts can be kept within reason, the shielding problems introduced by these ducts are not serious. At higher reactor power levels, however, the ducting problem may become extremely difficult, particularly with regard to neutron shielding.

A general analysis of the shield of the air cycle reactor as proposed in Report DC-51-9-28 is presented in the following pages. The conclusions are that the shield is adequate. Since some of the important features of the shield design in the report are marked "obsolete" however, this analysis is necessarily incomplete. Furthermore, NDA's careful study of the divided shield is not available in its final form for comparison and we possess only a preview of the essential features.

The gamma shielding features are essentially dependent on the adequacy of the procedure described in ANP-53.⁽¹³⁾ This procedure is being critically investigated in the above NDA study which, indicates that the computed shield weights are not conservative but probably correct. This statement might need some modification because of scattering by the aircraft structure and machinery. This scattering has been under investigation at NDA after the preview was obtained, and we do not know the results of the investigation. The results are also strongly dependent on the specific aircraft configuration, which is not known. The air cycle, with the necessity of placing a considerable amount of machinery close to the reactor is somewhat difficult in this respect.

The large airducts are not expected to be serious with regard to the penetration of gammas which are born in or near the reactor. The diameter, or width, of these ducts is small compared to the gamma relaxation length

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in water, and besides the gamma attenuation expected from the reactor shield is not very large. If the ducts are treated by a "distributed density" calculation, their effect on the above gammas would adequately be taken care of.

With regard to the shielding of neutrons the problem is more serious than in the ANP-53 design (which is, according to NDA, slightly conservative), because of the ducts. Only a few general considerations of the effect of these ducts can be stated.

Since the duct problem is the less serious, the smaller the attenuation expected from the reactor shield, other things being equal (at large attenuations the neutrons with exceptional histories become dominant), it appears favorable to put as much neutron shielding on the crew compartment as possible. This is somewhat in contradiction to the principle of the divided shield. In particular NDA looks unfavorably on the neutron shielding at the sides of the crew compartment, because the hydrogenous material is a very poor gamma shield at that location. This is so, because this material does only degrade, but not eliminate gammas besides it deflects some of the gammas in a direction perpendicular to the crew shield so that the energy loss is counter-balanced by the short path through the lead. However, if it is considered that the hydrogenous material is also a poor gamma shield at the reactor location (because of the gamma energy), then the above argument against the hydrogenous material at the crew shield sides becomes less cogent. The following factors may be of more concern.

If the neutron flux becomes very large, the capture gammas of the structural material, etc. in the neighborhood of the crew compartment will create excessive dosage inside the crew compartment. It will probably be hard to suppress these gammas by boron at the location of this structural material.

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It also will be very expensive weightwise to shield against these gammas, because they are hard and a sizeable fraction of them are approximately perpendicular to the crew shield. Hence, an upper limit for the permissible neutron flux at the crew compartment is given by these capture γ 's. Even at neutron fluxes below this upper limit, capture gammas in the hydrogenous crew shield itself may give an excessive dose inside the crew compartment. These gammas are also hard to shield against, but because of the simpler geometry, they probably can be suppressed easier by boron. It has to be remembered, however, that even capture in boron results in most cases in gammas of about 0.5 Mev, which also have a finite probability of penetrating the thin lead of the crew side shields. If this becomes troublesome, one could replace the boron by lithium, which captures neutrons without emission of gammas, (except for the rare gammas from capture in Li^7 , and the still rarer Bremsstrahlung from Li^8 β 's).

Thus some suppression of capture gammas in the hydrogenous crew shield itself seems possible either by B or Li. However, both these elements are poor attenuators of high energy neutrons, and particularly of neutrons in the 100 KeV region so that their insertion constitutes largely additional shield weight.

Once one has determined by considerations like the above, the n-flux permissible at the crew compartment, one can go back and compute the permissible n-flux at the reactor shield surface. This only requires the computation of the neutron scattering in air and the airplane itself, and this computation seems pretty much under control by NDA's work.

Then one is faced with the neutron penetration through the reactor shield, which is a solid basis, except for the ducts. The answer to the ducts is not available at present, and will eventually come from experiments at the Lid Tank and Bulk Shielding Facility. Theoretical work could be tried, once a certain

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configuration is at least tentatively decided on. Such theoretical work could probably be based on ray tracing, considering also first, second and third scattering; and possibly on some "first collision - and - aging down" theory as used by KAPL (Tonks, Hurwitz). Suppression of low energy neutrons by B could be attempted but here again the introduction of B effectively increases the void for n's around 100 Kev and above.

In conclusion, it should be pointed out that the proposed air cooled reactor is not anywhere near as sensitive to the duct shielding troubles as a reactor designed for specified high airplane performance. Since the reactor power is low to begin with, the required airflow and hence the duct size is relatively moderate. Any addition to the shield weight which might be required could, within limits, be compensated for by lower performance, as the proposed design only intends to get the airplane in the air. One thus avoids the vicious cycle which occurs if the performance is specified. Then, higher shield weight requires more reactor power, more airflow, more ducts, more shield weight.

The fact remains, that the large airducts with their shielding problem will present a serious obstacle to any high performance, biologically shielded aircraft based on a direct cycle.

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S E C R E T

Figure 1

Relative Thermal Flux and U Concentration for Flat Power and Uniform Loading

Normalized to same total power with stainless to uranium constant.

Figure 2

Fractional Escape of Radiation from Edge of a Thin Disk Source

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Figure 3

Fractional Escape of Radiation from the Ends of
Cylindrical Fuel Channels in Air Cycle Reactor

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Figure 4
Ratio of Mean Square Lengths to Square of Mean
Lengths in Long Channels of Rectangular
Cross Section