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SUBJECT: Steady-State Pressure and Thermal Stresses
in the HRT Pressure Vessel

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FROM: R. D. Cheverton and E. R. Volk

ABSTRACT

This report re-evaluates the methods used to calculate the steady-state pressure and thermal stresses in the spherical portion of the HRT pressure vessel. The stresses reported here were calculated using the methods considered most applicable at the present time. These methods yield stresses significantly greater than originally reported for the HRT because the calculated total heat generation rate in the vessel wall is greater, and its distribution is different.

A comparison of two-region (D_2O reflector) and single-region operation is made, showing very little difference between them insofar as stresses are concerned.

A limited amount of experimental heat balance data for the HRT vessel wall has been compared to the calculated values. Primarily on the basis of this comparison, recommendations are made concerning further procurement of experimental data.

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Introduction

The possible desire to operate the HRT as a single-region reactor required that the thermal stresses be calculated for that particular case. The stresses calculated in this report are the steady state pressure and thermal stresses in a clad spherical vessel, neglecting all discontinuities such as nozzles and reinforcements. All stresses are assumed to be symmetrical about the center. Since the thermal stresses are indirectly a function of the rate of heat removal from the outside surface of the vessel wall, the required heat removal rates for a given stress level have been calculated and compared to experimental heat flux data obtained during operation of the reactor.

A general report on the calculation of steady state heat generation, resultant thermal stresses, and pressure stresses in clad vessels will be issued in the near future by the same authors. For this reason, many of the detailed calculations have been omitted from this report.

Heat Generation:

Two main sources of heat generation were considered: capture gammas in the vessel wall and fission, decay, and capture gammas from the core and blanket.

The attenuation of gammas through the core, reflector, and vessel wall, and the distribution of energy deposition in the vessel wall were calculated by application of the NDA build up factors^{1, 2}. The results of these calculations show that the contribution to heating in the wall from gammas born outside the wall is about equal to that of the gammas released within the wall. Variations in the relative magnitudes of the two sources of heat generation are dependent upon the neutron flux distribution in the core and reflector and upon the method used to attenuate the gammas. Although there is no direct reference to the

original heat generation calculations specifically for the HRT, Chapman's work³ indicates that present calculations for core and reflector gamma heating in the wall result in significantly higher values than previously obtained and used⁴ for the HRT.

When calculating the capture gamma heating in the wall, it is necessary to know the neutron current into the wall and its distribution through the wall. The multi-group "Eyewash" code was used to obtain the neutron currents into the wall. The fast currents were summed to give one average fast group with an average energy equal to

$$\bar{E} = \sum_1 J_1 E_1 / \sum_1 J_1 \quad (1)$$

Two group diffusion theory was then used to determine the neutron flux distribution through the wall.

Table 1 lists some of the pertinent heat generation data for the two reactors.

The large difference in total heat generation between Chapman's value and present values is nearly all attributed to the difference in the core and reflector gamma contribution. Figure 1 compares the magnitude and distribution of heat generation in the HRT vessel wall as determined by Chapman and this study.

For either reactor it is possible to vary the relative magnitudes of the core and reflector gamma heating and the capture gamma heating by varying the quantity J_2/P . The number of gammas born in the core and reflector regions is primarily dependent upon the fission rate, whereas the number of gammas born in the wall at a given power level is primarily dependent upon the neutron currents at the inner surface of the wall and, therefore, upon the shape of the thermal neutron flux in the regions. For a given power level (given fission rate) the fuel concentration

TABLE 1

	Two-Region (D ₂ O reflector)		One-Region
	Present values	Chapman's	
$G(r = a)/J_2, \frac{\text{watts}}{\text{cm}^3} / \frac{\text{neuts}}{\text{cm}^2 \text{ sec}}$			
Gammas from core and reflector	1.97×10^{-13}		2.81×10^{-13}
Capture gammas in wall	1.48×10^{-13}		1.62×10^{-13}
$Q/J_2, \text{watts} / \frac{\text{neuts}}{\text{cm}^2 \text{ sec}}$	1.393×10^{-7}	0.634×10^{-7}	1.962×10^{-7}
$J_2/P; \frac{\text{neuts}}{\text{cm}^2 \text{ sec}} / \text{Mw}$	0.0482×10^{13}	0.044×10^{13}	0.0354×10^{13}
J_1/J_2	0.07	-	0.51
$\bar{E}_1, \text{ev}$	2.44	-	2.63×10^3

$G(r = a)$ = heat generation rate per unit volume in wall at inside surface

J_1 = fast neutron leakage into wall

J_2 = thermal neutron leakage into wall

$\bar{E}_1$ = average energy of fast neutrons entering wall

P = reactor power

Q = total heat generation rate in wall.

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Fig. 1. Comparison of heat generation curves in the HRT (D₂O blanket) vessel wall as used in ref. (4) and this study.

could be increased in order to decrease the thermal flux and, therefore, the capture gamma heating in the wall. Poison would have to be added to the reactor to maintain criticality at a desired temperature.

Heat Transfer and Temperature Distribution:

Solution of the heat generation problem does not result in a general equation expressing heat generation in terms of the variable distance through the wall. When determining the temperature distribution analytically, it is necessary that such an equation be used. It is often convenient to represent the heat generation distribution in the wall with one or more exponentials. However, with respect to the calculated heat generation curve a more accurate result was obtained for this study using numerical analysis.

Calculations have been made for two different boundary conditions on the outside surface of the wall: (1) outside surface perfectly insulated, (2) outside surface at any arbitrary temperature. For boundary condition (2) heat must of course be added to or removed from the outside surface. Only the case of heat removal is of interest. The difference between the inside and outside surface temperatures depends upon the relative amounts of heat removed from the two surfaces for a given total amount of heat generated in the wall. For the HRT the heat from the outside surface is removed essentially by radiation to the blast shield and by free convection between the pressure vessel wall and the blast shield. On the inside surface of the wall heat was assumed to be transferred by free convection only, because of the low velocity in the blanket. The effects of heat generation in the coolant were neglected.

Results of the heat transfer calculations are shown in Figs. 2, 3, and 4. Some of the equations used are found in Appendix I. The independent variable selected for several of the curves in this report is the difference between the inside and outside surface temperatures of the composite vessel wall. The advantage of this is that the heat fluxes and also the temperature gradient stresses can be determined once the temperature difference is known, without having to know anything about the heat transfer mechanisms on the two surfaces. The accuracy will then depend essentially upon the accuracy with which the heat generation and temperature difference across the wall can be calculated or determined experimentally.

A comparison of calculated and experimentally determined heat fluxes on the outside surface of the vessel wall has been made using limited experimental data from the HRT. The results are shown in Fig. 5. The general equation for the heat flux is

$$\frac{q_c}{A_c} = Pxf(H) - \left[\frac{k}{(c-a)} \right]_{\text{effective}} (T_c - T_a) \quad (2)$$

where $f(H)$ is some function of the heat generation and obviously, when multiplied by reactor power, represents the amount of heat generated in the wall that is removed from the outside surface when the temperature difference across the wall is zero. The last term which is independent of power level represents the heat transferred across the wall as a result of the temperature difference across the wall. The quantity in brackets is, of course, the slope of the heat flux curve in Figs. 2 and 5 and is dependent only on the geometry and heat conductance of the wall materials. Therefore, if it is assumed that these latter properties are not significantly different for the actual reactor vessel than for the analytical

Fig. 2. Surface heat fluxes vs. temperature difference across wall for one and two region HRT.

Fig. 3. Temperature difference across inside surface fluid film vs. temperature difference across wall for one and two region HRT.

Fig. 4. Blast shield temperature required to produce given temperature difference across pressure vessel wall.

Fig. 5. Comparison of calculated and experimental outer surface heat flux for the HRT.

model, one has a little assistance in fitting a curve to the widely scattered experimental points. However, if one attempted to fit a best straight line, considering all the points, a greater effective value of $\left[k/(c-a) \right]$ would be obtained and would have to be explained. A simple, though not very realistic, explanation would be that the cladding, with its low value of k and about eleven percent of the total wall thickness, had been removed. Naturally, a much more reasonable explanation would be that inadequate means are being used to obtain the necessary experimental data. As indicated in Fig. 5 one set of data gives a straight line slope essentially equal to the analytical solution while another gives a decidedly greater slope. Certainly the effective value of $\left[k/(c-a) \right]$ did not change during the short period of time that lapsed.

The so-called experimental value of $(T_c - T_a)$ involves an estimation of the ΔT across the film on the inside surface of the vessel wall. Actual values of neither the film conductance coefficient nor heat flux at the inside surface are known. The ΔT 's used in this report were determined using a calculated free convection film coefficient and a heat flux equal to the difference between the calculated total heat generation and the experimental outer surface heat flux. This procedure probably gives a larger ΔT across the film than actually exists. Therefore, if two curves such as in Fig. 5 are plotted, one using the estimated ΔT and the other assuming $\Delta T = 0$, the actual curve should be parallel to and between the two limiting curves. This has been done for two cases in Fig. 5.

The above curves are useful in comparing actual and theoretical heat generation rates in the wall. When $(T_c - T_a) = 0$, equation (2) and therefore Fig. 5 give the portion of the total heat generated in the wall that leaves the outside surface and that is independent of the temperature difference. If it

is assumed that the actual spatial distribution of heat generation is essentially the same as the theoretical, then a good estimate of the actual total heat generation can be obtained from curves similar to those in Fig. 5. It is probably meaningless, however, to try extracting such information from the presently available experimental data because of the wide scatter of points.

The theoretical curves in Fig. 4 appear to differ considerably from measured results. The differences might be attributed to errors in one or more of at least three calculations: total heat generation, relative magnitudes of inside and outside heat flux for a given ΔT across the wall, and heat transferred from vessel to blast shield for a given temperature difference between the two surfaces. The latter calculation checks very well with measured values.⁵ Therefore, the indications are that calculated values of total heat generation and/or the outer surface heat flux are too large.

Stresses:

Stresses in a clad vessel result from applied loads such as pressure, from temperature gradients in the wall, and from uniform temperature changes.⁶ This report considers pressure stresses and radial temperature gradient stresses in a sphere, everything being symmetrical about the center.

The magnitude of stresses resulting from uniform temperature changes in a clad vessel depends upon the magnitude of the residual stresses after complete manufacturing of the vessel. For the purposes of this study it has been assumed that at a uniform temperature of approximately 600°F the residual stresses in the vessel are zero. Experimental data⁷ indicate that the assumption is valid unless perhaps the vessel is thermal cycled an undetermined number of times. If an adequate number of cycles has been experienced by the vessel, one can assume the stresses to be zero at essentially room temperature. Using the first assumption,

the maximum tensile stress in the vessel wall at operating conditions occurs in the cladding, provided the outer surface temperature is somewhat greater than the inner surface temperature. Using the second assumption, the cladding is in compression at operating conditions and the base material tensile stresses are significantly greater than in the former case. However, relative to the allowable stresses for the cladding and base materials the stresses resulting from the first assumption are greater. Of course, there is considerable question as to whether the cladding stresses should be considered insofar as failure of the vessel is concerned. Lacking experimental verification one way or the other, it seems one must consider cladding stresses to be equally important.*

The magnitude of thermal gradient stresses is a linear function of the temperature difference between the inner and outer surfaces of the vessel wall, the minimum stresses occurring when the two temperatures are nearly equal. The calculated stresses are shown in Figs. 6 and 7.

Since two different materials are used in a clad vessel, it is necessary to consider four allowable stress values for the vessel. In accordance with a special ruling⁸ by the ASME Boiler Code's Special Committee on Nuclear Power, the allowable stress for a vessel subjected to both pressure and thermal gradients is one and one-half times the "S" value,⁹ provided the pressure stress does not exceed the "S" value. Therefore, the following allowable stresses at 600°F have been considered for the HRT vessel:

* This and related cladding questions are being investigated by a special ASME Pressure Vessel Code Committee.

Fig. 6. Temperature gradient stresses in vessel wall vs. temperature difference across wall for one and two region HRT.

Fig. 7. Tangential temperature gradient stresses vs. reactor power and blast shield temperature for one region HRT.

	Base Material	Cladding
	SA-212-B	S.S. 347
Pressure Stress, psi	17,500	14,900
Pressure + Thermal Stress	26,250	22,350

Conclusions:

Insofar as this report is concerned, there is only one important final result: the magnitude of the stresses. Of course, the inside surface or film temperature is important, but this is being investigated in greater detail by others.

Since the stresses are directly a function of only the shape and magnitude of the temperature curve and the temperature difference across the wall, only these latter values need to be known accurately. A fair estimation of the ΔT across the wall can be obtained from accurate measurements of the outer surface temperature and the average blanket temperature, if Fig. 3 is used to estimate the ΔT across the inside film for D_2O in the blanket region. With fuel in the blanket region, one must consider the effects of heat generation in the film and the possibility of fissionable material deposition on the wall. Either of these conditions tends to increase the inner surface temperature. If, however, with these conditions prevailing the reactor is operated so that the outer surface temperature is higher than the inner surface temperature, as determined by the above method, the actual ΔT across the wall will be less than that obtained when the outer surface temperature is less than the inner surface temperature. Whether or not the actual ΔT across the wall is less than the estimated ΔT depends on how accurate the calculation of the ΔT across the film is. An over estimation of the latter value could result in a greater actual ΔT across the wall than estimated.

The shape of the temperature curve for a given ΔT across the wall depends upon the magnitude and spatial distribution of the heat generation. Results so far indicate that the calculated magnitude is probably conservative, but to what extent it is difficult to say without knowing what the heat flux is on the inside surface. Therefore, the curves in Fig. 6, which are based on the theoretical heat generation rates, should be used. Figure 7 should not be considered of great importance.

Recommendations:

In view of the difficulties encountered in attempting to compare the experimental data with calculated values, and considering the advantages to be gained if one can conclusively show that calculated values of heat generation are overly conservative, it is recommended that plans be made for obtaining adequate experimental data from the HRT replacement vessel, if not from the present vessel.

By measuring only the radial temperature distribution through the wall of the vessel essentially all of the necessary data can be obtained. Knowing the shape of the temperature curve permits calculation of the thermal gradient stresses; the slopes of the temperature curve at the two boundaries give the desired heat fluxes; and by making a point-by-point heat balance along the temperature curve the magnitude and spatial distribution of the heat generation in the wall is obtained. The latter calculation would be made as follows:

$$\left[q \right]_{n \rightarrow (n-1)} = \left[\frac{kA}{\Delta x} \right]_{n \rightarrow (n-1)} (T_n - T_{(n+1)})$$

$$\left[q \right]_{(n+1) \rightarrow n} = \left[\frac{kA}{\Delta x} \right]_{(n+1) \rightarrow n} (T_{(n+1)} - T_n)$$

$$\therefore q_n = \left[q \right]_{n \rightarrow (n-1)} - \left[q \right]_{(n+1) \rightarrow n}$$

$$\text{and } G_n = q_n / V_n$$

The above calculation assumes that no heat is flowing transverse to the indicated direction.

There are probably several satisfactory methods by which the radial temperature distribution could be measured. One method is shown in Fig. 8.

Figure 8

A cylindrical core about one inch in diameter made of clad and base material as shown is inserted in the wall and welded at the inside surface. The thermocouples are in shallow longitudinal grooves or in a central hole, the tips protruding to the surface. Intimate contact between the core and wall is not necessary since the heat flow transverse to the axis of the core is negligible.

Appendix I

The following equations were used in the calculation of the temperature drop across the film on the inside surface and in the calculation of the temperature of the blast shield:

Inside surface free convection:

$$q_a = h A_a \Delta T_f$$

$$h = .15k \left(\frac{g\beta \Delta T_f \rho^2 C_p}{\mu k} \right)^{1/3} \quad (4), (10)$$

$$\therefore \Delta T_f = \left[\frac{q_a/A_a}{.15 k \left(\frac{g\beta \rho^2 C_p}{\mu k} \right)^{1/3}} \right]^{3/4}$$

The constants for 280°C D₂O or 10 gm/kg fuel were obtained from reference (4).

$$\rho = 51.88 \text{ \#/ft}^3$$

$$\mu = 0.266 \text{ lbs/hr-ft}$$

$$\beta = 0.00115 \text{ } ^\circ\text{F}^{-1}$$

$$k = 0.337 \text{ Btu/hr ft } ^\circ\text{F}$$

$$C_p = 1.238 \text{ Btu/lb } ^\circ\text{F}$$

$$g = 4.17 \times 10^8 \text{ ft/hr}^2$$

Outside surface radiation and free convection to blast shield:

$$\frac{q_c}{A_c} = \frac{\sigma (T_c^4 - T_d^4)}{\frac{1}{\epsilon_c} + \left(\frac{c}{d}\right)^2 \left(\frac{1}{\epsilon_d} - 1\right)} + .3 \left(\frac{T_c - T_d}{2}\right)^{1.25} \quad (4), (11)$$

$$\epsilon_c = \epsilon_d = .8$$

$$c = 34.5 \text{ in.}$$

$$d = 37.0 \text{ in.}$$

$$\sigma = 0.174 \times 10^{-8} \text{ Btu/hr ft}^2 \text{ } ^\circ\text{R}^4$$

The values for the heat fluxes used in the above equations were calculated from the following equations which were arrived at using theoretical heat generation rates:

One region:

$$\frac{q_c}{A_c} = 963.0 P \frac{\text{Btu}}{\text{hr ft}^2 \text{ Mw}} - 49.0 (T_c - T_a) \frac{\text{Btu}}{\text{hr ft}^2 \text{ } ^\circ\text{F}}$$

$$\frac{q_a}{A_a} = 1,747.0 \frac{\text{Btu}}{\text{hr ft}^2 \text{ Mw}} + 64.5 (T_c - T_a) \frac{\text{Btu}}{\text{hr ft}^2 \text{ } ^\circ\text{F}}$$

Two region:

$$\frac{q_c}{A_c} = 874.0 \times P \frac{\text{Btu}}{\text{hr ft}^2 \text{ Mw}} - 49.0 (T_c - T_a) \frac{\text{Btu}}{\text{hr ft}^2 \text{ } ^\circ\text{F}}$$

$$\frac{q_a}{A_a} = 1,774.0 P \frac{\text{Btu}}{\text{hr ft}^2 \text{ Mw}} + 64.5 (T_c - T_a) \frac{\text{Btu}}{\text{hr ft}^2 \text{ } ^\circ\text{F}}$$

Table of Nomenclatures:

A	= Heat flow area
a	= Inside surface radius of vessel
b	= Radius at interface between cladding and base material
c	= Outer surface radius of vessel
d	= Inside surface radius of blast shield
C_p	= Specific heat
ϵ_c	= Emissivity of outer surface of vessel
ϵ_d	= Emissivity of inner surface of blast shield
$\bar{E}_1$	= Average energy of fast neutrons
g	= Gravitational constant
G	= Power density in vessel wall
h	= Heat transfer coefficient
J_1	= Total fast neutron current into vessel wall
J_2	= Total thermal neutron current into vessel wall
k	= Thermal conductivity
P	= Reactor power
Q	= Total heat generation rate in vessel wall
q	= Heat flow rate
$\frac{q_a}{A_a}$	= Heat flux at inside surface of vessel wall
$\frac{q_c}{A_c}$	= Heat flux at outer surface of vessel wall
r	= Vessel radius
T_a	= Temperature of inside surface of vessel wall

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Table of Nomenclature (Cont'd)

T_b	=	Bulk mean temperature of blanket region
T_c	=	Temperature of outside surface of vessel wall
T_d	=	Temperature of inside surface of blast shield
ΔT_f	=	Temperature drop across inside surface film
V	=	Volume
β	=	Volumetric coefficient of expansion
μ	=	Viscosity
ρ	=	Density
σ	=	Stefan-Boltzmann's constant
$(\sigma_{tap})_{ab}$	=	Tangential pressure stress, inside surface, in cladding
$(\sigma_{tbp})_{ab}$	=	" " " , interface, in cladding
$(\sigma_{tbp})_{bc}$	=	" " " , " , in base material
$(\sigma_{tcp})_{bc}$	=	" " " , outer surface, in base material
$(\sigma_{taT})_{ab}$	=	" thermal gradient stress, inside surface, in cladding
$(\sigma_{tbT})_{ab}$	=	" " " " , interface, in cladding
$(\sigma_{tbT})_{bc}$	=	" " " " , " , in base material
$(\sigma_{tcT})_{bc}$	=	" " " " , outer surface, in base material

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