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**DESIGN EVALUATION OF IN-REACTOR TUBE
ARGONNE WATER LOOP AT MTR (ANL-2)**

By
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March 1955

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Technical Information Service, Oak Ridge, Tennessee

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DESIGN EVALUATION OF IN-REACTOR TUBE
FOR
ARGONNE WATER LOOP AT MTR (ANL-2)

by

L. W. Fromm

Reactor Engineering Division

March, 1955

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DESIGN EVALUATION OF IN-REACTOR TUBE FOR ARGONNE WATER LOOP AT MTR. (ANL-2)

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ABSTRACT

A rigorous stress analysis is performed on a pressurized, in-reactor tube test facility (Type 347 stainless steel tube with Type 316 tip) to ensure conformity of the design with ASA and ASME Codes. Detailed calculations, along with tabulated and graphical data, show that the maximum membrane stress (14,200 psi) and the combined membrane and thermal stresses (22,200 psi) are well below the yield point of the steel. Calculated thermal gradients are lower than those accepted in commercial practice. Experimentally determined stresses and temperature gradients verify or are more favorable than corresponding calculated values.

Radiation damage data are presented to show that even at saturation levels, the properties of Type 300 series stainless steels do not approach the limits at which embrittlement failure might be anticipated.

I. INTRODUCTION

A high-temperature, high-pressure circulating water loop known as ANL-2 has been installed in the Materials Testing Reactor as a part of the Central Station Water Reactor and Submarine Thermal Reactor programs. The facility permits the simultaneous exposure of specimens, principally fuel element prototypes, to high-level radiation and to flowing high-temperature water.

Briefly, the loop consists of an in-reactor tube, in which the samples are exposed, a heat exchanger for removing heat generated within the samples and the in-reactor tube itself, pumps for circulating the water and heaters for control of water temperature, together with appropriate means for control of gas, solid, and ionic content of the water, for pressure regulation, and for measuring important parameters of the system. The in-reactor tube is located in Hole HB-2 of the reactor; the related equipment is situated in the basement of the reactor building.

From the design standpoint, the most critical portion of the loop is the innermost end of the in-reactor tube, commonly referred to as the tube tip. In designing the tube tip it was necessary to effect a compromise between the heavy wall thickness required to contain the design pressure, and the light wall thickness required to minimize thermal gradients through the metal resulting from gamma heating.

The design of the tube tip was based on the following criteria and assumptions:

- (1) Design pressure: 1650 psig
- (2) Operating water temperature: 600F
- (3) Gamma heating in the MTR reflector¹ multiplied by a factor of 1.26 to convert from heating in beryllium to heating in stainless steel. Figure 1 shows the gamma heating values as a function of distance from the innermost end of the tube tip. The values represent conditions in the MTR operating at 30-mw power level.
- (4) No heat loss or gain from the outside surface of tube or tip.
- (5) Maximum allowable stresses defined as follows:

<u>Stainless Steel AISI Type</u>	<u>Max. Allowable Pressure Stress at 600F²</u>	<u>Max. Allowable Combined Stress at 600F,* psi</u>
316	17100	25575
347	14900	22275

The tube tip was originally designed for fabrication from Type 347 stainless steel. A calculation was first made to determine the minimum thickness (with an ID of 3.068 in.) required to contain the design pressure. Based upon the maximum allowable pressure stress of 14900 psi, this thickness was 0.180 in. Considering this as the thickness of the cylindrical portion of the tip, thermal gradient calculations were then carried out first to determine the gradient through this thickness at the junction of the cylindrical and hemispherical portions, and then to determine the thicknesses at various points in the hemispherical portion which would produce the same thermal gradient. The final design (Fig 2) was the result of the reduction of these calculated thicknesses to terms of machinable dimensions.

¹Superscript numbers refer to literature references listed in the Bibliography.

* Maximum allowable combined stresses due to bending and pressure were calculated per ASA Piping Code (ASA B31.1 - 1952), Section 6, paragraph 620(g). Thermal stresses are considered to be equivalent to bending stresses.

FIG. 1
GAMMA HEATING IN STAINLESS STEEL
IN MTR HOLE HB-2

RE-7-15716-A
L.W.FROMM:L.B.,4-22-55

FIG. 2
IN-REACTOR TUBE TIP FINAL DESIGN

RE-6-15720-A
F.W.KLEIMOLA:L.B., 4-22-55

It should be emphasized that the entire design was based upon the allowable stresses for Type 347 stainless steel, and that the material specification was later changed to Type 316 in order to provide an additional factor of safety in allowable stress at the elevated temperature.

The prime purpose of this report is to present the exhaustive stress analysis performed on the tube tip design to ensure that the requirements of the ASME and ASA codes were met adequately by the design. In addition, an analysis of measured thermocouple data is presented to show that actual gamma heating and/or film gradients, and consequently stresses, are probably considerably less than the calculated values.

II. SUMMARY AND DISCUSSION OF RESULTS

A. Stresses

The combined stress at any point in the tube tip is the algebraic sum of the membrane and thermal stresses at that point. In turn, the total membrane stress is the algebraic sum of the direct pressure stress and the discontinuity stress necessary to restrain differences in deflection at the point of junction between the cylindrical and hemispherical portions of the tube.

The methods used to calculate the pressure, discontinuity, and thermal stresses in the different portions of the tube tip are detailed in Appendix A. All calculations were based on the design criteria and assumptions described previously.

Analysis showed that the maximum discontinuity stresses occur in the cylindrical portion 0.333 in. from the junction of the two portions and in the hemispherical portion 0.323 in. from the junction. Therefore, stresses have been calculated at the inner and outer surfaces both at these two points and at the innermost tip end. These points are indicated in Fig. 3; the results of the calculations are given in Table I. The table also lists the equations (in Appendix A) used to compute each stress value. The physical and mechanical properties of AISI Type 316 stainless steel used in these calculations are given in Appendix B.

As pointed out earlier in the design criteria, the maximum allowable pressure stress for Type 316 stainless steel at 600F is 17,100 psi, and the maximum allowable combined stress is 25,575 psi. It can be seen from Table I that in all cases the membrane stresses and combined stresses calculated for points of maximum stress in the in-reactor tube and tip are less than the applicable allowable values. Therefore it may be concluded that Code requirements are met by this design.

An experimental stress analysis was performed on a second tube tip (fabricated for experimental purposes) identical with the tube tip used in ANL-2 except for a circumferential weld in the cylindrical portion about one inch from the junction. This weld does not exist in the ANL-2 tip. Strain measurements were made in both the tangential and longitudinal direction at six different locations on the outer surface of the duplicate tip with application of internal hydrostatic pressures up to 5000 psig. Pressures were applied in increments, and interim measurements were made at zero pressure in order to detect any permanent set. Stresses were calculated from the strain measurements. Details of the test are given in Appendix C.

FIG. 3
POINTS IN TUBE TIP FOR WHICH
STRESSES WERE CALCULATED

RE-6-13078-A
L. W. FROMM: L. R. D., 6-7-54

Table I

CALCULATED STRESSES AT 30-MW REACTOR OPERATION

(Internal Pressure 1650 psig)

Point of Interest	Gamma Heat (watts/ gram)	Location	Ref. Point (Fig. 3)	Membrane				Thermal Stress		Total Combined Stress, σ	
				Pressure		Discontinuity		Total σ	σ		(Eq)*
				σ	(Eq)*	σ	(Eq)*				
Tangential Stresses, σ_t (psi)											
Cylinder 0.333 in. from junction	5.1	inside	A	14900	(1)	-710	(28)	14200	8000	(5)	22200
		outside	B	13300	(2)	710	(28)	14000	-6100	(6)	7900
Hemisphere 0.323 in. from junction	6.0	inside	C	7420	(8)	780	(28)	8200	11200	(15)	19400
		outside	D	6590	(9)	-780	(28)	5810	-4900	(16)	900
Hemisphere tip end	8.3	inside	E	9210	(8)	-	-	9210	9700	(15)	18900
		outside	F	8390	(9)	-	-	8390	-4400	(16)	4000
Longitudinal Stresses, σ_z (psi)											
Cylinder 0.333 in. from junction	5.1	inside	A	6640	(3)	-2360	(27)	4280	8000	(5)	12300
		outside	B	6640	(3)	2360	(27)	9000	-6100	(6)	2900
Hemisphere 0.323 in. from junction	6.0	inside	C	7420	(8)	2600	(27)	10000	11200	(15)	21200
		outside	D	6590	(9)	-2600	(27)	3990	-4900	(16)	-900
Hemisphere tip end	8.3	inside	E	9210	(8)	-	-	9210	9700	(15)	18900
		outside	F	8390	(9)	-	-	8390	-4400	(16)	4000

*Equation in Appendix A used to compute stress value.

Although the points of measurement and calculation do not exactly coincide, the results are still comparable. Table II lists the stresses at various points as determined from strain measurements at an internal pressure of 1500 psig, as well as the calculated total membrane stresses from Table I (adjusted from 1650 to 1500 psig pressure). The results are in fair agreement with the exception of the tip end and the point 1-1/4 in. from the junction in the cylindrical portion. The latter error is probably explained by the proximity of the point to the extra weld mentioned. In all cases, measured values appear to fall below the calculated stresses.

It should be noted that an internal pressure of 3000 psig was required to create a stress at any measured point beyond the 17,000 psi allowable pressure stress for Type 316 stainless steel (see Table III, Appendix C). Furthermore, even at a pressure of 5000 psig the yield point of the material was not exceeded. The maximum stress-strain offset at any location was 0.1315%, which is well below the 0.2% offset usually accepted as the yield point for stainless steels.

B. Thermal Gradients

A total of 25 thermocouples were welded to the outside surface of the in-reactor tube and tip before insertion of the unit into the reactor. The locations of these thermocouples are shown in Figs 4 and 5. The locations were chosen to permit measurement of surface temperatures at all points considered critical from the viewpoint of heat generation, heat transfer, and combined stress, and to facilitate determination of the change in temperature with axial distance from the tip end.

For points on the tube and tip lying in location planes I through X on Fig. 5, thermal gradients through the metal wall were calculated, using Eqs. (7) and (13) in Appendix A. For planes V through X, thermal gradients through the inside water film were also calculated, using methods detailed in Appendix D. No film gradients could be computed for planes I through IV because of the unknown nature of the water flow pattern in the hemispherical portion of the tip.

Figure 6 shows the calculated and measured metal, film, and total gradients from bulk water to outside metal surface at the various thermocouple location planes. For the first four planes only the estimated shape of the curves for film gradient and total gradient is shown (in dashed lines), since no film gradient calculations could be made in this zone. Also plotted are the average total gradients estimated from four separate sets of thermocouple readings taken on four separate days during reactor operation at 30-mw power level. The equipment for reading the thermocouples was set up and the measurements made by members of the MTR operating staff. Actual readings, along with details of conversion, averaging, and correction, are presented in Appendix E.

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Table II

COMPARISON OF MEASURED AND CALCULATED MEMBRANE STRESSES

(Internal Pressure 1500 psig)

		Hemispherical Portion			Cylindrical Portion	
	<u>Tip End</u>	<u>11/32" (0.344) from Junction</u>	<u>0.323" from Junction</u>	<u>At Junction</u>	<u>0.333" from Junction</u>	<u>1-1/4" from Junction</u>
<u>Tangential Stresses, σ_t (psi)</u>						
Measured*	5000	4500	-	6900	-	8650
Calculated**	7550	-	5230	-	12600	-
<u>Longitudinal Stresses, σ_z (psi)</u>						
Measured*	5000	2800	-	4750	-	4300
Calculated**	7550	-	3590	-	8100	-

*From strain measurements - see Appendix C.

**From Table I - adjusted from 1650 to 1500 psi internal pressure.

FIG. 4
IN-REACTOR TUBE
TIP THERMOCOUPLES

FIG. 6
CALCULATED AND MEASURED THERMAL GRADIENTS

The shape of the measured total gradient curve compares well with the shape of the calculated curve; however, the measured values are much lower. The discrepancy may be caused by errors in assumed gamma heating values, calculated heat transfer coefficients, or both.

The following important observations may be made from Fig. 6:

- (1) The calculated values for the thermal gradient through the metal wall are all less than 50F. For commercial pressure vessels this is an accepted gradient below which detailed calculations are customarily omitted.
- (2) The measured total gradients fall below the calculated values for the metal wall alone.
- (3) Both measured and calculated data produce curves which are very smooth and contain no abrupt variations which might indicate the existence of stress concentrations in the tip.

It is therefore concluded that the design is safe from the thermal gradient standpoint.

C. Radiation Damage

Although data on the effect of neutron irradiation of stainless steels to exposures about 10^{20} nvt fast is limited, sufficient data are available to show that the ANL-2 in-reactor tube and tip have been designed within a safe range.

Irradiation is known to increase both the tensile strength and hardness of stainless steels; however, a number of investigators have shown that the degree of damage tends to saturate with exposure and that further irradiation produces little additional change. For Type 300 series stainless steels irradiated at room temperature, saturation seems to occur in the range of Brinell Hardness Number (BHN) 200-240. Irradiation effects have been shown to be dependent upon temperature of irradiation, with increased temperature diminishing the radiation damage associated with a given exposure.

A more complete discussion of the irradiation effects in stainless steels, together with measured data and references, will be found in Appendix F.

An increase in tensile strength is, of course, beneficial to design, and it should be noted that an increase of hardness to the BHN 220-240 range by no means places the steel in danger of embrittlement failure. The latter would not be expected until the steel reached the fully embrittled state corresponding to a BHN of 740. It should further be remembered that allowable

strengths delineated in piping and boiler codes are based on the premise that in the average vessel there will be uncalculated discontinuity stresses set up by side openings and piping connections in tanks, etc., and that these discontinuities will be absorbed by the ductility of the metal. In the tube and tip design for ANL-2 no such discontinuities exist except for the transition from cylindrical to hemispherical portion in the tip, and this factor has been accounted for in the stress analysis. Applying the normal factor of safety to the ANL-2 tube thereby results in an even safer design than found in normal pressure vessels, and certainly counteracts any loss of ductility as a result of irradiation.

III. CONCLUSIONS

A rigorous stress analysis of the ANL-2 in-reactor tube tip shows that maximum membrane stresses at 1650 psig internal pressure (10% higher than normal operating pressure) are well below the allowable membrane stress. The sums of the membrane and thermal stresses at points of maximum stress are well below the maximum allowable combined membrane and bending stress. The design therefore meets Code requirements.

Results of an experimental stress analysis verify the calculated membrane stresses at the normal operating pressure of 1500 psig.

Calculated thermal gradients across the metal wall in the tip are all less than 50F. For commercial pressure vessels this is an accepted gradient below which detailed calculations are customarily omitted.

The total metal and water film gradients measured by MTR personnel fall below the calculated values for the metal wall alone. This may indicate errors in the pessimistic direction in the calculated heat transfer coefficients, the assumed gamma heating values, or both. If the assumed gamma heating values are too high, then the thermal stresses are even less than those calculated in this report.

Both measured and calculated thermal gradient data produce curves which contain no abrupt variations which might indicate the existence of stress concentrations.

The process of radiation damage to the Type 300 series stainless steel proceeds at a decreasing rate and approaches a saturation level. At this level, the Brinell Hardness of the material (BHN 220-240) is still sufficiently below the total embrittlement range (BHN-740) that no danger from brittle fracture exists.

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APPENDIX A

METHODS OF STRESS CALCULATIONI. CYLINDRICAL PORTIONA. Membrane Stresses

The membrane stresses were calculated from standard formulas for stresses in internally pressurized thick-walled cylinders.³

At inner surface:

$$\sigma_{t_a} = p \frac{b^2 + a^2}{b^2 - a^2} \quad (1)$$

At outer surface:

$$\sigma_{t_b} = 2p \frac{a^2}{b^2 - a^2} \quad (2)$$

At both surfaces:

$$\sigma_z = p \frac{a^2}{b^2 - a^2} \quad (3)$$

where

σ_{t_a} = tangential stress at inner surface

σ_{t_b} = tangential stress at outer surface

σ_z = longitudinal stress at both surfaces

p = internal pressure

a = inside radius

b = outside radius

B. Thermal Stresses

The expression for tangential stress, σ_t , in a cylinder in which heat is generated uniformly and from which heat is removed only from the inside surface is given by Carter:⁴

$$\sigma_t = \frac{\alpha E q}{16k(1-\nu)r^2} \left[\frac{r^2 + a^2}{b^2 - a^2} \left(4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4 \right) - 2b^2 r^2 \left(1 + 2 \ln \frac{r}{a} \right) + 2a^2(b^2 - r^2) + 3r^4 - a^4 \right] \quad (4)$$

where

α = coefficient of thermal expansion

E = Young's modulus of elasticity

q = volumetric heat generation rate

k = thermal conductivity

ν = Poisson's ratio

a = inside radius

b = outside radius

Since the tangential and longitudinal stresses have their highest numerical values at the inner and outer surfaces, and since at each surface $\sigma_t = \sigma_z$,⁵ Eq. (4) may be reduced as follows:

For the inner surface ($r = a$),

$$\sigma_{t_a} = \sigma_{z_a} = \frac{\alpha E q}{8k(1-\nu)(b^2 - a^2)} \left[4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4 \right] \quad (5)$$

For the outer surface ($r = b$),

$$\sigma_{t_b} = \sigma_{z_b} = \frac{\alpha E q}{16k(1-\nu)b^2} \left[\frac{b^2 + a^2}{b^2 - a^2} \left(4b^4 \ln \frac{b}{a} - 3b^4 + 4a^2b^2 - a^4 \right) - 4b^4 \ln \frac{b}{a} + b^4 - a^4 \right] \quad (6)$$

For a relationship for the thermal gradient θ across the metal wall, another expression given by Carter⁴ may be used:

$$\theta = \frac{q}{2k} \left[b^2 \ln \frac{r}{a} - \left(\frac{r^2 - a^2}{2} \right) \right]$$

which for the case of the total gradient ($r = b$) reduces to

$$\theta = \frac{q}{2k} \left[b^2 \ln \frac{b}{a} - \left(\frac{b^2 - a^2}{2} \right) \right] \quad (7)$$

II. HEMISPHERICAL PORTION

A. Membrane Stresses

The stresses were calculated from standard formulas for stresses in internally pressurized thick-walled spheres.³

At inner surface:

$$\sigma_{t_a} = \sigma_{z_a} = p \frac{b^3 + 2a^3}{2(b^3 - a^3)} \quad (8)$$

At outer surface:

$$\sigma_{t_b} = \sigma_{z_b} = p \frac{3a^3}{2(b^3 - a^3)} \quad (9)$$

where the symbols have the same meanings as in previous expressions.

B. Thermal Stresses

The general expression for thermal stress in a hollow sphere may be expressed as⁶

$$\sigma_t = \sigma_z = \frac{2\alpha E}{1 - \nu} \left[\frac{2r^3 + a^3}{2(b^3 - a^3)r^3} \int_a^b \theta r^2 dr + \frac{1}{2r^3} \int_a^r \theta r^2 dr - 1/2 \theta \right] \quad (10)$$

where θ = temperature difference from inner to outer surface. All other symbols have the same meaning as in previous expressions.

Carter⁷ gives a general relationship for θ in spheres:

$$\theta = -\frac{qr^2}{6k} + \frac{C_1}{r} + C_2 \quad (11)$$

where C_1 and C_2 are integration constants. All other symbols are as previously defined.

Applying boundary conditions to evaluate C_1 and C_2 for a hollow sphere with all heat removed at the inner surface,

$$\text{At } r = b, \frac{d\theta}{dr} = 0$$

$$\text{At } r = a, \theta = 0$$

$$\frac{d\theta}{dr} = -\frac{q}{6k} (2r) - \frac{C_1}{r^2}$$

Setting this equal to zero and substituting for r ,

$$\frac{qb}{3k} + \frac{C_1}{b^2} = 0$$

$$C_1 = -\frac{qb^3}{3k}$$

Substituting the second set of boundary conditions in Eq. (11),

$$0 = -\frac{qa^2}{6k} + \frac{C_1}{a} + C_2$$

Substituting the expression for C_1 , and solving for C_2 :

$$0 = -\frac{qa^2}{6k} - \frac{qb^3}{3ka} + C_2$$

$$C_2 = \frac{q}{3k} \left(\frac{a^2}{2} + \frac{b^3}{a} \right)$$

Substituting the expressions for C_1 and C_2 in Eq. (11)

$$\theta = -\frac{q}{3k} \left[\left(\frac{r^2 - a^2}{2} \right) + b^3 \left(\frac{1}{r} - \frac{1}{a} \right) \right] \quad (12)$$

which for the case of total θ across the wall ($r = b$) reduces to

$$\theta = -\frac{q}{3k} \left[\left(\frac{b^2 - a^2}{2} \right) + b^3 \left(\frac{1}{b} - \frac{1}{a} \right) \right] \quad (13)$$

Substituting Eq. (12) in Eq. (10) and integrating, we have

$$\begin{aligned} \sigma_t = \sigma_z = \frac{\alpha E q}{3k(1-\nu)} \left\{ \left[\frac{2r^3 + a^3}{(b^3 - a^3)r^3} \right] \left(\frac{a^2 b^3}{3} - \frac{a^5}{15} + \frac{b^6}{3a} - \frac{3b^5}{5} \right) \right. \\ + \left(\frac{1}{r^3} \right) \left(\frac{r^3 a^2}{6} - \frac{r^5}{10} + \frac{b^3 r^3}{3a} - \frac{b^3 r^2}{2} + \frac{b^3 a^2}{6} - \frac{a^5}{15} \right) \\ \left. + \left(\frac{r^2 - a^2}{2} \right) + b^3 \left(\frac{1}{r} - \frac{1}{a} \right) \right\} \quad (14) \end{aligned}$$

For stresses at the inner surface, where $r = a$, the expression becomes:

$$\sigma_{ta} = \sigma_{za} = \left(\frac{\alpha E q}{k(1-\nu)(b^3 - a^3)} \right) \left(\frac{a^2 b^3}{3} - \frac{a^5}{15} + \frac{b^6}{3a} - \frac{3b^5}{5} \right) \quad (15)$$

For stresses at the outer surface ($r = b$):

$$\sigma_{tb} = \sigma_{zb} = \frac{\alpha E q}{3k(1 - \nu)b^3} \left[\left(\frac{2b^3 + a^3}{b^3 - a^3} \right) \left(\frac{a^2 b^3}{3} + \frac{b^6}{3a} - \frac{3b^5}{5} - \frac{a^5}{15} \right) - \frac{a^2 b^3}{6} + \frac{9b^5}{10} - \frac{2b^6}{3a} - \frac{a^5}{15} \right] \quad (16)$$

III. IMMEDIATE VICINITY OF CYLINDER-HEMISPHERE JUNCTION

A. Discontinuity Stresses

For a given pressure and for a given membrane stress, the required wall thickness in a sphere is only about one-half that required in a cylinder. However, in this design, the thicknesses of the hemispherical and cylindrical portions adjacent to the junction are equal; therefore the theoretical displacements in the hemisphere and in the cylinder resulting from the internal pressure application would differ materially from each other if the two portions were unjoined. When the portions are joined, discontinuity stresses which restrain the differing displacements are set up. With reference to the illustration, the magnitudes of these stresses are calculated as follows:

(1) Calculation of theoretical inside displacements, considering the two portions not joined: (3)

$$u_c = p \frac{a}{E} \left[\frac{b^2 + a^2}{b^2 - a^2} - \nu \left(\frac{a^2}{b^2 - a^2} - 1 \right) \right] \quad (17)$$

$$u_h = p \frac{a}{E} \left[\frac{b'^3 + 2a^3}{2(b'^3 - a^3)} (1 - \nu) + \nu \right] \quad (18)$$

where

u_c = inside displacement of cylinder

u_h = inside displacement of hemisphere

b' = outside radius assumed for hemisphere based on average thickness of portion subtended by a 30-degree angle adjacent to junction.

(2) Calculation of forces and moments restraining differing displacements:

Let

V_0 = the shear force

M_0 = the bending moment which restrains the difference in displacement ($u_c - u_h$)

u' = the portion of ($u_c - u_h$) taken up by the cylinder

and

u'' = the portion of ($u_c - u_h$) taken up by the hemisphere .

Standard relationships for radial displacement of a cylinder under uniform radial shear and uniform radial moment give⁽⁸⁾

$$u' = \frac{V_0}{2D_c \lambda_c^3} - \frac{M_0}{2D_c \lambda_c^2} \quad (19)$$

and since relationships for a figure of revolution reduce to the cylindrical case for hemispheres,⁽⁹⁾ the expression for u'' (in this case V_0 acts outward) is

$$u'' = \frac{V_0}{2D_h \lambda_h^3} + \frac{M_0}{2D_h \lambda_h^2} \quad (20)$$

where

$$D = \frac{Et^3}{12(1 - \nu^2)}$$

$$\lambda = \sqrt[4]{\frac{3(1 - \nu^2)}{R^2 t^2}}$$

t = wall thickness

R = mean radius

and subscripts (c) and (h) refer to cylinder and hemisphere respectively.

In this case

$$t_c = \frac{a + b}{2}$$

and

$$t_h = \frac{a + b'}{2}$$

Since the slopes of the wall of the two portions must be equal:

$$-\frac{V_0}{2D_c \lambda_c^2} + \frac{M_0}{\lambda_c D_c} = \frac{V_0}{2D_h \lambda_h^2} + \frac{M_0}{\lambda_h D_h}$$

or

$$V_0 \left(-\frac{1}{2D_c \lambda_c^2} + \frac{1}{2D_h \lambda_h^2} \right) - M_0 \left(\frac{1}{\lambda_c D_c} + \frac{1}{\lambda_h D_h} \right) = 0 \quad (21)$$

Adding the relationship

$$u' + u'' = u_c - u_h, \quad (22)$$

where u_c and u_h have been determined from Eqs. (17) and (18), Eqs. (19), (20), (21), and (22) may be solved simultaneously for V_0 and M_0 .

(3) Calculation of maximum discontinuity stress:

The combination of V_0 and M_0 exerts a moment M_x which reaches its maximum not at the junction but at points on either side of the junction. M_x in this case represents the algebraic summation of the moments caused by V_0 and by M_0 : (8)

$$M_{xc} = \frac{e^{-\lambda_c x}}{\lambda_c} \left[V_0 \sin \lambda_c x - M_0 (\cos \lambda_c x + \sin \lambda_c x) \right] \quad (23)$$

$$M_{xh} = \frac{e^{-\lambda_h x}}{\lambda_h} \left[V_0 \sin \lambda_h x + M_0 (\cos \lambda_h x + \sin \lambda_h x) \right] \quad (24)$$

The distance x from the junction at which the maximum moment is exerted is found by differentiating Eqs. (23) and (24) with respect to x , setting equal to zero, and solving for x :

$$\begin{aligned} \frac{dM_{xc}}{dx} = 0 &= e^{-\lambda_c x} \left[V_0 (\cos \lambda_c x - \sin \lambda_c x) + 2M_0 \sin \lambda_c x \right] \\ x_c &= \frac{1}{\lambda_c} \arcsin \sqrt{\frac{V_0^2}{(V_0 - 2M_0)^2 + V_0^2}} \end{aligned} \quad (25)$$

and

$$\begin{aligned} \frac{dM_{xh}}{dx} = 0 &= e^{-\lambda_h x} \left[V_0 (\cos \lambda_h x - \sin \lambda_h x) - 2M_0 \sin \lambda_h x \right] \\ x_h &= \frac{1}{\lambda_h} \arcsin \sqrt{\frac{V_0^2}{(V_0 + 2M_0)^2 + V_0^2}} \end{aligned} \quad (26)$$

The maximum moments are then found by substituting values of x_c and x_h in Eqs. (23) and (24). Stresses caused by these moments are determined from the formulas: (8)

$$\sigma_z = \frac{6M}{t^2} \quad (27)$$

and

$$\sigma_t = \nu \sigma_z = \frac{6M \nu}{t^2} \quad (28)$$

B. Membrane and Thermal Stresses

The method described for the cylindrical portion (page 21) or for the hemispherical portion (page 23) can be applied, depending upon the location of the point of interest. On the hemispherical side, the value of b is calculated as a function of the location of the point of interest.

APPENDIX BPROPERTIES OF AISI TYPE 316 STAINLESS STEEL

The following mechanical and physical properties of AISI Type 316 stainless steel at 600F were used in stress calculations in this report:

<u>Property</u>	<u>Symbol</u>	<u>Value</u>	<u>Bibliog. Reference</u>
Young's Modulus of Elasticity	E	25 x 10 ⁶ psi	10, 11
Thermal Conductivity	k	10.3 Btu/(hr)(sq ft)(ft/F)	12, 13
Coefficient of Linear Thermal Expansion	α	9.0 x 10 ⁻⁶ in./(in.)(F)	12, 13, 14, 15
Poisson's Ratio	ν	0.3	13
Density	ρ	7.87 gm/cc	13*

*The room temperature value was adjusted to 600F by application of the coefficient of cubical thermal expansion. The latter was taken as three times the linear coefficient given above. The relationship used was

$$\rho_{600F} = (\rho_{70F}) \left[\frac{1}{1 + (3)(\alpha)(530)} \right]$$

APPENDIX C

EXPERIMENTAL STRESS ANALYSIS IN DUPLICATE TUBE TIP

An experimental stress analysis was performed on a duplicate to the tube tip used in the ANL-2 in-reactor tube. The two units were identical in every respect except that the duplicate was fabricated in two pieces and welded together circumferentially in the cylindrical portion about one inch from the junction of hemispherical and cylindrical portions.

Ten strain gages were attached to the duplicate in-reactor tube tip as illustrated in Fig. 7. The arrangement was such as to enable measurement of both tangential and longitudinal strain in the hemispherical portion at the tip end (where strain is equal in all directions) and in planes $5/8$ in. and $11/32$ in. from the junction. The latter point corresponds closely with the calculated point of maximum membrane stress in the hemispherical portion. In the cylindrical portion gages were placed $1-1/4$ in. from the junction and on the weld simulating the joint between the tip and the in-reactor tube proper. Gages were also placed at the junction itself.

The test procedure consisted of raising the pressure to the desired point (as measured on a calibrated pressure gage), reading the strain gages, allowing the pressure to fall to atmospheric, and reading the strain gages again. Readings were obtained in 500 psi increments up to 2000 psig, and in approximately 250-psi increments from 2000 to 5000 psig.

Results of the strain measurements, when expressed as surface stresses, are given in Table III. Permanent set measurements are plotted in Fig. 8 for the four points which showed the highest tangential stresses. The maximum deviations from stress-strain proportionality (per cent offset) for these four points are listed below.

<u>Gage</u>	<u>Location</u>	<u>% Offset</u>
4	Hemisphere $11/32$ in. from junction	-0.0002
10	At junction	0.0155
1	At tip end	0.0586
7	Cylinder $1-1/4$ in. from junction	0.1315

With internal pressures as high as 5000 psig, the metal surface stress computed from strain gage measurements did not exceed the yield strength of the material in any location. The maximum stress-strain offset at any location was 0.1315%, which is below the 0.2% offset usually accepted as the yield point for stainless steels. Maximum principal stresses (corrected) ranged from 4500 psi at the weld (gage 8) to 38,800 psi circumferential at a level

FIG. 7
STRAIN GAGE POSITIONS FOR
EXPERIMENTAL STRESS ANALYSIS

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Table III

EXPERIMENTAL STRESSES (PSI) IN TYPE 316 DUPLICATE TUBE TIP

Internal Hydraulic Pressure (psig)	Hemispherical Portion					At Junction		Cylindrical Portion		
	At Tip End σ_t, σ_z (1)	5/8" From Junction		11/32" From Junction		σ_t (10)	σ_z (5)	1-1/4" From Junction		At Weld σ_z (8)
		σ_t (9)	σ_z (2)	σ_t (4)	σ_z (3)			σ_t (7)	σ_z (6)	
500	1900	1400	1600	1400	950	2350	1420	2850	1350	250
1000	3300	2900	2950	3350	1900	4500	3500	5400	3350	500
1500	5000	4150	4300	4500	2800	6900	4750	8650	4300	950
2000	7000	5600	5750	6125	3700	9050	6650	11350	5750	1250
2250	7800	6400	6700	7000	4150	11000	7850	13200	6650	1400
2500	8100	6900	6950	7750	4650	11750	8350	14350	7150	1550
2775	9100	7200	7900	8450	5150	13000	9150	16200	8250	2000
3000	10000	8500	8500	9250	5550	14000	10150	17350	8750	1950
3260	12000	9850	9850	10600	6700	15600	11300	19700	10250	2850
3500	12000	9850	9950	10800	6500	16400	11500	20850	10400	2550
3750	14800	10500	10500	11350	6800	17500	12100	21700	10850	2400
4000	14450	11600	11500	12150	7050	18900	13000	25800	12650	2800
4240	15300	11900	11900	12700	7450	20000	13650	27700	13850	3000
4475	16600	12800	12500	13300	7750	21200	14350	31400	15500	3550
4725	17900	13600	13500	14600	8250	22300	14750	35200	17350	4500
5000	18500	13900	14000	14850	8500	23300	15500	38800	19700	3900

Note: σ_t = tangential stress; σ_z = longitudinal stress.

Numbers in parentheses refer to strain gage numbers indicated in Fig. 7.

Stress values shown have been corrected for transverse sensitivity, gage factor, and effect of Poisson's ratio.

FIG. 8
PERMANENT SET IN DUPLICATE TUBE TIP

RE-7-15718-A
L.W. FROMM: L.B., 4-22-55

1-1/4 in. below the junction of the sphere and the cylinder (gage 7). The stress at the tube tip (gage 1) for 5000 psig internal pressure was 18,500 psi, whereas the circumferential stress at the junction of sphere and cylinder (gage 10) was 23,300 psi.

The permanent set after the 5000 psig pressure ranged from a slight contraction to an expansion on the 3-1/2 inch diameter of only 0.0046 in. Below 3150 psig pressure, the permanent set was negligible at all positions.

APPENDIX D

THERMAL FILM GRADIENT CALCULATIONS

For that transfer by convection from a metal surface to a flowing liquid, the familiar equation is

$$q = h A \theta_f$$

or

$$\theta_f = \frac{q}{Ah} \quad (29)$$

where

q = heat transferred per unit time

h = heat transfer coefficient

A = area of heat transfer surface

θ_f = film gradient (temperature difference between metal surface and bulk liquid)

The heat transfer coefficient, h , for turbulent liquids flowing in heated pipes is given by McAdams;¹⁶

$$h = 0.023 \frac{k}{D} \left(\frac{Du\rho}{\mu} \right)^{0.8} \left(\frac{c\mu}{k} \right)^{0.4} \quad (30)$$

where

k = thermal conductivity

D = diameter of pipe*

u = linear velocity

ρ = density

μ = viscosity

c = specific heat

* In flow passages other than circular, the equivalent diameter D_{eq} is used, where

$$D_{eq} = 4 \left(\frac{\text{flow area}}{\text{wetted perimeter}} \right)$$

Assuming that all heat generated within the metal wall is removed from the inside surface, for a unit length the heat transferred per unit time is

$$q = g\pi(b^2 - a^2)$$

where

g = volumetric heat generation rate

b = outside radius

a = inside radius

For the same unit length, the area of heat transfer surface is

$$A = 2\pi a$$

Substituting these relationships in Eq. (29), we have

$$\theta_f = \frac{g(b^2 - a^2)}{2ah} \quad (31)$$

Although Eq. (31) has been developed for the cylindrical case, it can be shown that the equation is also an adequate approximation for the part of the hemispherical portion of the tip lying adjacent to the cylindrical portion. Since no calculations of film gradients could be made in the hemispherical portion near the tip end because of obscurity of flow patterns, no spherical relationship for gradient was developed.

The results of the calculations of the heat transfer coefficient h , and of the film gradient θ_f , the metal wall gradient θ_m , and the total gradient θ_t , are given in Table IV. The reduced values of h at distances 1.297 and 1.672 were caused by the increased equivalent diameter D_{eq} and the decreased flow velocity resulting from the physical configuration of the nozzle end of the rectangular tube inside the in-reactor tube. Film gradients, θ_f , were calculated from Eq. (31). Metal wall gradients, θ_m , were obtained from Eqs. (7) and (13) in Appendix A. In each case the total gradient, θ_t , is the sum of θ_f and θ_m . Values of the gradients calculated here, as well as measured gradients from Appendix E, are plotted in Fig. 6 on page 17.

Table IV

CALCULATED THERMAL GRADIENTS

Distance From Tip End, in.	Thermocouple No.	Plane No.	Depth, in.	Flow Velocity, fps	h , Btu/(hr)(sq ft)(F)	Gradient, F		
						θ_f	θ_m	θ_t
0	1	I		-	-	-	43.0	-
0.062	2	II		-	-	-	43.7	-
0.156	3,4	III		-	-	-	43.9	-
0.625	5-8	IV		-	-	-	46.2	-
1.297	9-13	V	1.031	6.47	2642.	26.3	48.0	74.3
1.672	14	VI	1.023	6.45	2649.	25.0	48.3	73.3
1.80			0.961	7.49	3016.	21.4	46.1	67.5
1.95						20.6	44.4	65.0
2.047	15-19	VII				20.0	43.1	63.1
2.2						19.5	41.8	61.3
2.4						18.4	39.6	58.0
2.6						17.7	37.9	55.6
2.797	20	VIII				17.1	37.5	54.6
3.797	21	IX				15.3	36.8	52.1
4.797	22	X				13.6	36.1	49.7

APPENDIX E

THERMOCOUPLE MEASUREMENTS

At the time of fabrication of the in-reactor tube, twenty-five chromel-alumel thermocouples were welded to the outside surface of the tube and tip in order to permit detection of high thermal gradients which might result from poor water flow distribution within the tube. The locations of these thermocouples are shown in Figs. 4 and 5 on pages 15 and 16. Locations were chosen to facilitate measurements at all points considered critical from the viewpoint of heat generation, heat transfer, and combined stress.

Equipment for the accurate reading of these thermocouples was set up by MTR personnel, and readings were taken periodically by the MTR operating staff. A number of sets of millivolt data were furnished by R. B. Johns of the MTR staff. Of these sets, four were accompanied by water flow and bulk water temperature data. These four sets of data were converted to degrees Fahrenheit by the standard tables and were used as the basis for obtaining measured total gradients. The data supplied by MTR (converted to degrees F) are listed in Table V.

The test cartridge in the in-reactor tube at the time of the temperature readings generated about 56 kw of heat with the reactor at full power. This heat generation caused a calculated 5.6F temperature rise in the water passing through, at a flow rate of 98 gpm. Since the water must pass through the cartridge before flowing over the inside of the tube tip, the figure of 5.6F was added to the bulk water temperature measured at the tube inlet, and the resulting sum was subtracted from the surface temperatures to obtain the total gradients. The gradients are listed in Table VI, along with average gradients for each plane of thermocouples for each set of data. Also included in the table are the average gradients from the four sets for each plane of couples. The latter averages are plotted in Fig. 6 on page 17.

In order to verify the averaged values shown in Table VI, all gradients were plotted and the shapes of the curves were compared. In cases of gross inconsistency between the shape of the curves, the original data were replaced with values read from the curves; these values are indicated with a single asterisk. On a similar basis, the data marked with a double asterisk were not included in the averages because of inconsistency with other readings in the same plane of thermocouples.

Table V

MEASURED TUBE AND TIP SURFACE TEMPERATURES

(Converted from millivolt readings supplied by MTR)

Thermocouple Number	Temperature, F			
	Set No. 1	Set No. 2	Set No. 3	Set No. 4
1	548.6	551.5	543.4	548.9
3	547.0	551.6	548.5	552.0
4	546.7	549.2	541.4	547.0
5	542.5	546.3	540.1	546.4
7	545.9	550.5	546.9	550.4
8	542.8	547.9	542.6	546.3
9	541.6	545.2	539.5	544.2
10	543.3	548.2	541.8	547.7
11	543.9	548.1	541.4	546.6
12	542.5	546.3	538.2	543.4
13	543.0	547.6	540.2	546.3
14	541.7	546.8	540.3	546.7
15	536.7	542.8	535.2	541.4
20	535.9	542.0	534.2	540.3
21	539.9	543.9	540.5	544.1
22	536.0	541.3	532.9	539.7
Bulk water at tube inlet	508	508	504	502
Water flow, gpm	97.5	97.5	98	98

Table VI
MEASURED TOTAL GRADIENTS

Thermocouple Number	Plane No.	Total Thermal Gradient, F				Average
		Set No. 1	Set No. 2	Set No. 3	Set No. 4	
1	I	35.0	37.9	36.7*	42.9*	38.1
3		33.4	38.0	38.9	44.4	
4		33.1	35.6	31.8	39.4	
3, 4 avg.	III	33.3	36.8	35.4	41.9	36.9
5		28.9	32.7	30.5	38.8	
7		32.3	36.9	37.3**	42.8**	
8		29.2	34.3	33.0	38.7	
5-8 avg.	IV	30.1	34.6	31.8	38.8	33.8
9		28.0	31.6	29.9	36.6	
10		29.7	34.6	32.2	40.1	
11		30.3	34.5	31.8	39.0	
12		28.9	32.7	28.6	35.8	
13		29.4	34.0	30.6	38.7	
9-13 avg.	V	29.3	33.5	30.6	38.0	32.9
14	VI	28.1	33.2	30.7	39.1	32.8
15	VII	23.1	29.2	25.6	33.8	27.9
20	VIII	22.3	28.4	24.6	32.7	27.0
21	IX	26.3	30.3	30.9	36.5	31.0
22	X	22.4	27.7	23.3	32.1	26.4

*Obtained from curves. Original data inconsistent with other curves and with logic.

**Not included in average. Inconsistent with other points in same plane.

APPENDIX F

EFFECT OF IRRADIATION ON TYPES 316 AND 347 STAINLESS STEEL

A number of investigators have indicated that the process of radiation damage proceeds at a decreasing rate and approaches a saturation level. This phenomenon is illustrated in Fig. 9 where data from various sources are plotted to show changes in hardness, ultimate tensile strength, and ductility as functions of fast neutron exposure. Two curves have been drawn for ultimate tensile strength and for ductility since, in several cases, the types of samples used differed so widely that results are not directly comparable. However, the effect of approaching a saturation level can be seen in all cases.

Other investigations have shown the effect of irradiation temperature on the changes in properties. In a test made by KAPL,²² two hardness samples of Type 347 stainless steel, annealed, were irradiated in a controlled temperature slug at Chalk River for an integrated thermal neutron flux of 2×10^{20} nvt (fast flux approximately 1/2 this value) at approximately 775F. The maximum hardness increase produced by the irradiation was only 8 BHN. Under the same flux conditions but at a temperature of only 200F, identical specimens increased in hardness by 82 BHN, confirming the observations of other investigators that radiation damage is reduced as the temperature of irradiation is increased.²³

Three survey reports^{20,24,25} on the effects of irradiation on materials show that the rate and extent of alteration of properties of different materials under particle bombardment will depend on the number of primary and secondary dislocations, and on the number of atoms that do not return to normal positions in the lattice. The number and magnitude of these dislocations, in turn, are a function of the intensity and time of irradiation exposure until a structure saturated with dislocations or transformations emerges. After saturation, irradiation has negligible effect. These surveys also indicate that overall damage is alleviated to some extent by temperatures that fall within the stress-relieving range of the material being irradiated.

The very nature of radiation damage makes the effects not so much a function of small changes of chemical compositions but rather of the microstructure of the material exposed. Data reported in one of the surveys²⁰ indicates that results of radiation damage on any of the Type 300 series stainless steels can be expected for others in this series.

DATA	SST TYPE	IRRADIATION EST. TEMP, F	BIBLIOG. REF.
△	347	200	17
▲	347, ANNEAL.	325	20
□	347	150	18
○	347	570	19
●	316, ANNEAL.	70-80	21

FIG. 9
SATURATION OF RADIATION DAMAGE IN
TYPES 316 AND 347 STAINLESS STEEL

ANL-LWF-15719-A
L.W.FROMM:L.B., 4-22-55

BIBLIOGRAPHY

1. C. F. Leyse, "Facilities for Irradiations Within the MTR Reactor Tank," IDO-16048, June 5, 1953, p. 67.
2. "Maximum Allowable Stress Values for High Alloy Materials in Pounds Per Square Inch," Mechanical Engineering, 73, September, 1951, Table UHA-23, pp. 762-763.
3. R. J. Roark, "Formulas for Stress and Strain," (3rd ed.; New York: McGraw-Hill, 1954), p. 276.
4. J. C. Carter, "Temperature and Stress Distribution in Spheres, Rods, Tubes, and Plates in which the Heat Source is Within the Boundaries of the Solids," ANL-4690, September 7, 1951.
5. S. Timoshenko, "Theory of Elasticity," (1st ed.; New York: McGraw-Hill, 1934), p. 372.
6. Ibid., p. 379.
7. J. C. Carter, op. cit., p. 3.
Note that an erratum exists on the referenced page: Eq. (4) should read

$$\theta = -\frac{qr^2}{6k} + \frac{C_1}{r} + C_2$$
8. R. J. Roark, op. cit., p. 271.
9. R. J. Roark, "Formulas for Stress and Strain," (2nd ed.; New York: McGraw-Hill, 1953), p. 262.
10. M. H. Roberts and J. Nortcliffe, "Measurement of Young's Modulus at High Temperatures," J. Iron Steel Inst. (London), 157, Part 3 (November, 1947).
11. G. C. Seager and F. C. Thompson, "Influence of Temperature on Modulus of Elasticity of Plain Carbon and Alloy Steels," J. Iron Steel Inst. (London), 147 (1943).
12. R. F. Miller, G. V. Smith, and P. A. Jennings, "Alloy Steels for High Temperature Service," Metals and Alloys, 16 (1942).
13. "Metals Handbook," (American Society for Metals, 1948).
14. C. L. Clark, "High Temperature Properties of Steels," Lectures presented to ASM, 1945 Spring Lecture Course, San Francisco, Calif.

15. S. L. Hoyt, "Metal Data," (New York: Reinhold Publishing Co., 1952).
16. W. H. McAdams, "Heat Transmission," (2nd ed.; New York: McGraw-Hill, 1952), p. 168.
17. J. R. Low, Jr., et al, "Effect of Irradiation on the Mechanical Properties of Metals," Report of Metallurgy Section - June, July, August, 1952," KAPL-807, p. 65.
18. W. H. Pennington, "Materials Testing Reactor Progress Report - August, 1954," IDO-16192, September 9, 1954, p. 14.
19. Unpublished data, Argonne National Laboratory, Metallurgy Division.
20. D. O. Leeser, "The Effect of Irradiation on the Mechanical and Physical Properties of Structural Materials with Particular Reference to the Mark I Naval Reactor," ANL-4792, November, 1953, Experiment 9-S, p. 93.
21. Ibid., Experiment 5-S, p. 90.
22. J. R. Low, Jr., C. A. Bruch, and F. H. Sebring, "Effect of Irradiation on the Mechanical Properties of Metals," Report of Metallurgy Section - March, April, May, 1952, KAPL-750, p. 64.
23. D. O. Leeser, op. cit., p. 33.
24. Sidney Siegel, D. Billington, "Effect of Reactor Radiation on Metals," J. Metallurgy and Ceramics, TID-66, January 1949, p. 54.
25. B. L. Averbach, et al, "Survey of Effects of Irradiation on Materials," AEC-500, September, 1949.