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I-181202

DR# 0620-3

UCID--20165-Vol.3

DE85 002575

SPECIAL TOPICS REPORTS FOR THE REFERENCE
TANDEM MIRROR FUSION BREEDER: BERYLLIUM
LIFETIME ASSESSMENT

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October 1984

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SPECIAL TOPICS REPORTS FOR THE REFERENCE TANDEM MIRROR FUSION BREEDER

BERYLLIUM LIFETIME ASSESSMENT

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VOLUME 3 OF 6

October 1984

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PREFACE

This report represents Volume 3 of 6 volumes directed towards the resolution of special topics associated with a reference fission-suppressed tandem mirror fusion breeder reactor design developed during 1981-1982. A complete list of the special topics reports follows:

- Volume 1: Liquid Metal MHD Pressure Drop Effects In The Packed Bed Blanket by T. McCarville, D. Berwald, and C. P. C. Wong.
- Volume 2: Reactor Safety Assessment by I. Maya, C. G. Hoot, C. P. C. Wong, K. R. Schultz, J. K. Garner, S. J. Bradbury, W. G. Steele, and D. H. Berwald.
- Volume 3: Beryllium Lifetime Assessment by L. G. Miller, J. M. Beeston, B. L. Harris, and C. P. C. Wong.
- Volume 4: Structural Analysis by G. Orient, R. A. Westmann, N. Ghoniem, and J. K. Garner.
- Volume 5: Neutronics Issues and Optimization by J. D. Lee.
- Volume 6: Materials Compatibility Issues and Experimental Results by J. H. DeVan and P. Tortorelli.

The issue of beryllium pebble failure, due to irradiation damage induced deformations and/or changes in materials properties, is difficult to resolve analytically. This is because of uncertainties in the fabricated properties of the beryllium pebbles, the response of the material to irradiation at high temperatures in the fusion environment, the complexity of the pebble geometry, and variations in the pebble geometry history.

Although the beryllium pebble lifetime predicted in this report is, indeed, adequate, the analytical techniques used to predict that lifetime are conservative in several respects:

- o The tensile stresses due to differential swelling are overestimated.
- o The irradiation history of the pebbles ignores any differential swelling relaxation due to pebble re-orientation during irradiation.
- o The fracture lifetime-determining toughness and/or ductility of beryllium at the irradiation temperature may, in fact, be higher than assumed.
- o The level of tensile stress will decrease as a crack propagates toward the center of the pebble, tending to limit crack growth.

In one respect, the lifetime predictions may, however, be optimistic. Recent theoretical models of irradiation creep in beryllium indicate that this mechanism might not provide the level of stress relaxation which has been assumed. If no credit is taken for creep relaxation, the conservative lifetime estimates which follow must be reduced by 50%.

D. H. Berwald
October 1984

ABSTRACT

The lifetime of beryllium pebbles in the Reference Tandem Mirror Fusion Breeder blanket is estimated on the basis of the maximum stress generated in the pebbles. The forces due to stacking height, lithium flow, and the internal stresses due to thermal expansion and differential swelling are considered. The total stresses are calculated for three positions in the blanket, at a first wall neutron wall loading of 1.3 MW/m^2 . These positions are: (a) near the first fuel zone wall, (b) near the center, and (c) near the back wall. The average lifetime of the pebbles is estimated to be 6.5 years. The specific estimated lifetimes are 2.4 years, 5.4 years, and 15 years for the first fuel zone wall, center and near the back wall, respectively.

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SPECIAL TOPICS REPORTS FOR THE
REFERENCE TANDEM MIRROR FUSION BREEDER

BERYLLIUM LIFETIME ASSESSMENT

1. BERYLLIUM LIFETIME ISSUES

The beryllium lifetime estimates for the Tandem Mirror Fusion Breeder (TMFB) are based upon the ability of the metal to undergo plastic deformation. An increase in yield strength with irradiation is expected, and adequate fracture toughness at the operating temperatures of 300°C to 600°C^{1,2,3,4,5} is assumed. The loading stresses on the pebbles are produced from thermal expansion, differential swelling from irradiation, and external forces such as gravity and lithium pressure.

The data indicated that adequate ductility at operating conditions is present. The lifetime of the pebbles is estimated on the basis of the maximum stress theory, wherein failure occurs when one of the principal stresses from loading reaches the minimum yield strength in tension or compression of the irradiated beryllium. However, surface cracks, and defects that may propagate, must be stable according to the stress intensity factor and fracture toughness of the irradiated beryllium. If adequate ductility was not present, the Hertz contact stress would exceed the yield strength and produce failure.

2. DATA BASE EVALUATION AND EXPERIMENTAL RESULTS

2.1 Yield Strength and Ductility

The increase in yield strength upon irradiation is beneficial in the operating temperature range 300 to 600°C (Figure 1) as it allows a higher failure stress. As shown in Figure 1, at a fluence of 1.2×10^{22} n/cm² ($E > 1$ MeV) the irradiated compressive strength at a temperature of 750 K is higher than the unirradiated compressive or tensile strength.

The ductility¹⁻⁶ and fracture toughness⁷ properties which measure the ability of beryllium to plastically deform are relatively low for beryllium at temperatures <200°C. The question of failure and lifetime hinges upon the ductility and strength of the irradiated beryllium pebbles. Although the irradiated data base is sparse at high fluence and high temperatures, recent data⁸ on beryllium irradiated to 1.2×10^{22} n/cm² ($E > 1$ MeV) does provide an indication (Figure 2) that the plastic strain may be adequate in the operating temperature range of 300 to 600°C. Additional indications from postirradiation annealing tests⁹ and high temperature irradiation tests^{2,4,5,10} are that the defects may agglomerate with retention of some ductility. The retention of some ductility is necessary so that the pebbles do not act as indenters with one another to produce Hertzian contact cracking¹¹ and premature failure.

2.2 Irradiation Swelling (Elongation)

Swelling measurements have been made for beryllium specimens irradiated at various temperatures as well as for specimens irradiated at <400 K and postirradiation annealed. The swelling measured for specimens irradiated at various temperatures in EBR-II⁸ is compared to that estimated from postirradiation annealed specimens¹² in Table 1. For specimens irradiated at these temperatures, the swelling is given by

Figure 1. Yield strength of beryllium as function of test temperature.

Figure 2. Plastic strain of beryllium as function of test or irradiation temperature.

TABLE 1. COMPARISON OF CALCULATED SWELLING FROM RECOMMENDED EQUATION WITH ESTIMATED POSTIRRADIATION EQUATION

Fluence ^a ($n/cm^2 \times 10^{22}$) ($E > 1$ MeV)	Temperature T (K)	Swelling ^b ($\Delta V/V$) ^b	Equivalent Fluence ^b for Helium Production ϕ	Temperature Parameter X ($\frac{T^\circ C}{100}$)	Swelling ($\Delta V/V$) ^b	Ratio ($\Delta V/V$) ^b ($\Delta V/V$) ^a
1	673	0.0037	2.4	4.0	0.044	12.0
	773	0.0065		5.0	0.090	14.0
	873	0.0071		6.0	0.204	29.0
2	673	0.015	4.8	4.0	0.058	3.9
	773	0.025		5.0	0.104	4.0
	873	0.043		6.0	0.218	5.1
3	673	0.034	7.2	4.0	0.073	2.1
	773	0.059		5.0	0.119	2.0
	873	0.096		6.0	0.233	2.5
4	673	0.060	9.6	4.0	0.088	1.5
	773	0.105		5.0	0.133	1.3
	873	0.170		6.0	0.247	1.5
5	673	0.094	12.0	4.0	0.102	1.1
	773	0.163		5.0	0.148	0.9
	873	0.266		6.0	0.262	1.0
6	673	0.135	14.4	4.0	0.116	0.9
	773	0.235		5.0	0.162	0.7
	873	0.383		6.0	0.276	0.7
7	673	0.24	19.2	4.0	0.147	0.6
	773	0.42		5.0	0.193	0.5
	873	0.68		6.0	0.507	0.5

a. Equation is $\Delta V/V = 1.83 \times 10^{-58} \phi^{2.4}$.

b. Equation is $\Delta V/V = 0.00549 \phi + 1.035 + 7.8 \times 10^{-4} (2.5)^X$, where $X = \frac{T^\circ C}{100}$, and ϕ is equivalent fluence in units of $10^{22} n/cm^2$, $E > 1$ MeV.

$$\frac{\Delta V}{V} = 1.83 \times 10^{-58} \phi^2 T^4 \quad (1)$$

where

ϕ = fluence in n/cm^2 ($E > 1$ MeV)

T = temperature in K.

For the postirradiation annealed samples the swelling is given by

$$\frac{\Delta V}{V} = 0.00549 \phi^{1.035} + 7.8 \times 10^{-4} (2.5)^x \quad (2)$$

where

ϕ = fluence in units of $10^{22} n/cm^2$ ($E > 1$ MeV)

$x = \frac{T^\circ C}{100}$

Equations 1 and 2 give approximately the same swelling for certain temperatures and fluences as long as the fluences are equivalent fluences based on helium production. Table 2 indicates how helium production cross sections are used to obtain the equivalent fluences.

Equation 1 will be used to calculate swelling in the TMFB blanket. The linear expansion is taken as one-third the volume expansion. The reference fuel form upon which the loading stresses are applied is indicated in Figure 3.

2.3 Fracture Toughness of Irradiated Beryllium

The fracture toughness has been measured⁷ for beryllium irradiated at 66°C to fluence of $5 \times 10^{21} n/cm^2$ $E > 1$ MeV and tested at room temperature as $4.8 MPa \cdot m^{1/2}$ ($4.4 ksi \cdot m^{1/2}$) for full dense ($1.85 g/cm^3$) and $4.2 MPa \cdot m^{1/2}$ for 95% dense ($1.76 g/cm^3$) specimens. For beryllium with a tensile ductility of about 4% elongation, the fracture toughness was

TABLE 2. HELIUM PRODUCTION AND EQUIVALENT DAMAGE FLUENCE

Reactor	Normalized Helium Production Rate (at. ppm He per 10^{22} n/cm ² E > 1 MeV)	Helium Production Ratio ^a (Calculated to Measured) in EBR-II	TMHR Beryllium Zone Fluence (n/cm ² E > 1 MeV per year)	Equivalent ^b Damage Fluence (n/cm ² E > 1 MeV per year) $\times 10^{-22}$
Fusion Breeder				
Near first zone wall	6700-7000	4.5	0.71×10^{22}	1.28 or 3.20
Near center	5900-6200	4.0	0.24×10^{22}	0.38 or 0.96
Near back wall	5500-5800	3.8	0.038×10^{22}	0.057 or 0.14
ATR (Fission Reactor)	3700-4700	2.4	--	--
EBR-II (8E2 Position)	1410-1730	1.1	--	--
	(1540 Measured)	1.0	--	--

a. Second number assumes complete burnup of Li daughter into helium. First number is used in determination of ratio for conservatism.

b. Equivalent fluence; first number for postirradiation and second number for present swelling equation.

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Figure 3. Modified reference fuel form to enable gravitational indexing.

$12 \text{ MPa}\cdot\text{m}^{1/2}$ ($11.1 \text{ ksi}\cdot\text{in.}^{1/2}$). The fracture toughness of beryllium at temperatures above 400 K is not available, therefore an estimate is made for beryllium at TMFB blanket temperatures.

2.4 Fluence Distribution Through Blanket

The equivalent fluence distribution through the blanket is shown in Figure 4. It was obtained by a parabolic fit to the three equivalent fluences calculated: at the first wall ($3.2 \times 10^{22} \text{ n/cm}^2 E > 1 \text{ MeV}$ per year), near center ($0.96 \times 10^{22} \text{ n/cm}^2 E > 1 \text{ MeV}$ per year), and near back wall ($0.14 \times 10^{22} \text{ n/cm}^2 E > 1 \text{ MeV}$ per year) (Table 2).

Stresses for two different positions of the pebbles were calculated:
(1) pebble positioned by gravity as indicated in Figure 3, and
(2) positioned with the thorium snap-ring vertical. It was found that the maximum stresses occurred with the snap-ring positioned vertical.

Figure 4. Equivalent damage fluence distribution.

3. THERMAL MODELING

The steady-state temperature distribution of beryllium/thorium composite fuel balls was calculated using TACO 2D, a two-dimensional finite element heat transfer code. The results were displayed graphically using POSTACO, a post-processor for scalar two-dimensional finite element codes as illustrated in Figures 5 and 6. Both codes are available on the CRAY C and D machines of the Magnetic Fusion Energy Computer Center Network.

The balls consist of a beryllium neutron multiplier encircled by a thorium ring. Steady-state temperature profiles were calculated for balls with volumetric power generation levels corresponding to the front and the back of a lithium-cooled fission suppressed blanket. Because the individual balls are small (3 cm in diameter), uniform volumetric power generation rates in the beryllium and thorium were assumed.

Since there is no accepted parametric equation to calculate the convective heat transfer coefficient (h) between the balls and the lithium under a uniform magnetic field, three values of h at 1500, 3000, and 4500 $\text{W/m}^2 \text{K}$ were analyzed. The conservative value of 1500 $\text{W/m}^2 \text{K}$ corresponds to the removal of heat by conduction through the lithium at a separation of two ball diameters.

Table 3 summarizes the thermal inputs and the calculated temperature at the top of the ball (the point furthest away from the thorium) and at the side of the ball (the midplane of the beryllium/thorium interface). The results indicate that decreasing the convective heat transfer coefficient raises the beryllium ball temperature and slightly increases the temperature differential. The maximum temperature differential was 14°C.

Two other cases also were examined in which the power generation varied spatially throughout the ball. In the first case, the power generation decreased from the top to the bottom of the ball; in the second case the power generation decreased from one side of the ball to the other side, parallel to the ring. In the first case, the temperature throughout

Figure 5. Beryllium/thorium snap ring fuel element (front) isoplot temp/coolant.

Figure 6. Beryllium/thorium snap ring fuel element (back) isoplot temp/coolant.

TABLE 3. SUMMARY OF COMPOSITE BALL TEMPERATURE DISTRIBUTION

q''' (W/m ³)	h (W/m ² K)	T_{top} (°C)	T_{side} (Be/Th Interface)	$T_{coolant}$ (Lithium), (°C)	ΔT (°C)
190	1500	399	413	364	14.0
190	3000	379	392	364	13.0
190	4500	373	385	364	12.0
39.4	1500	454.0	457.5	448	3.5
39.4	3000	450.5	453.5	448	3.0
39.4	4500	449.5	452.0	448	2.5

the ball varied less than 1°C from the corresponding uniform power generation case. The second case becomes a 3-D problem and could not be represented by the TACO 2-D calculation. It can be noted that the primary thermal gradient in the beryllium is due to heat transfer from the thorium, through the beryllium, to the lithium. We estimate that the small azimuthal variation in local heating will produce only a very small azimuthal temperature gradient and thus will have a negligible effect on this larger gradient. Therefore, the indicated results should have closely approximated the maximum temperature gradient situation.

4. LIFETIME ANALYSIS

4.1 Design Consideration

The hybrid reactor blanket fuel zone is 0.40 m thick and divided into two 0.20 m thick annular zones filled with beryllium pebbles of 0.03 m diameter. A lithium plenum precedes the inner fuel zone. The stress analysis on beryllium pebbles considered the forces due to lithium flow and pebble stacking height, and the internal stresses due to thermal expansion and differential swelling.

4.2 Forces On The Pebbles

The hydraulic force due to the MHD pressure drop opposes the stacking pebble weight at the top of the fuel zone and is in the same direction at the bottom of the fuel zone. The hydraulic force (F) on the pebbles is approximated by considering the constraint force on the perforated zone separator sheet as a reaction force transferred through all the pebbles. The constraint force on the annulus grid is the pressure drop times the projected area, $F = \Delta P A$. The beryllium pebble reaction force is also applied on the projection area. The hydraulic force on each pebble of area πr^2 is

$$F = \Delta P \pi r^2 = 212 \text{ N} \quad (3)$$

where a 0.3 MPa pressure differential ΔP across each zone was assumed.

The force due to the weight of the pebbles is the density difference $\Delta \rho$ between lithium and beryllium times the acceleration of gravity times the volume. The height of the column above a pebble at the bottom of the blanket is 6 m, and the force on a pebble is

$$F = \Delta \rho g \pi r^2 h = 58 \text{ N} \quad (4)$$

Thus, the hydraulic force due to the flowing lithium was 3.7 times that of the weight of the pebbles.

4.3 Contact Stresses

The loading from hydraulic and gravitational force is large enough to deform or fail the beryllium contact points. Calculation is first made to determine the deformation (strain) that would occur. Figure 2 indicates the beryllium pebble has sufficient ductility to deform a small amount without fracture. The contact radius (a) of a pebble on the zone separator sheet is determined by the yield stress of the beryllium and the loading force. The yield stress σ_0 at high fluence and 500°C is taken as 276 MPa (Figure 1). The contact radius¹³ is given by

$$a = \left[\frac{3 (212 + 58)N}{2\pi \times 276 \times 10^6 \frac{N}{m^2}} \right]^{1/2} = 0.068 \text{ cm} \quad (5)$$

The radial strain, ϵ , corresponding to this deformation is

$$\epsilon = 1 - \frac{1}{r} (r^2 - a^2)^{1/2} = 1 - \frac{1}{1.5} \left[(1.5)^2 - (0.068)^2 \right]^{1/2} \quad (6)$$

$$= 0.00103 \text{ or } 0.1\%$$

Assuming the elastic equations can be used to estimate the maximum tensile stress σ_t near the contact point, using Poisson's ratio $\nu = 0.02$, and $\sigma_0 = 276 \text{ MPa}$.

$$\sigma_t = \frac{1 - 2\nu}{3} \sigma_0 = 87.5 \text{ MPa} \quad (7)$$

With a fracture toughness of $12.0 \text{ MPa}\cdot\text{m}^{1/2}$ (ductility approximately 3%), the depth of a surface crack (c) in the vicinity of the contact point which would be stable (not propagate) can be calculated from

$$K = \sigma (\pi c)^{1/2} \quad (8)$$

The maximum stable crack depth is $c = 0.62$ cm. If the fracture toughness drops to $4.8 \text{ MPa}\cdot\text{m}^{1/2}$, then the stable surface crack depth would decrease to 0.097 cm.

Now if the pebbles, because of irradiation hardening, behave elastically, the contact radius (a) between two pebbles¹³ would be determined, since Poisson's ratio $\nu = 0.02$ is small as:

$$a = \left(\frac{3Pr}{4E} \right)^{1/3} \quad (9)$$

$$\text{so that } a = \left(\frac{3 \times 270 \text{ N} \times 0.015 \text{ m}}{4 \times 275.8 \times 10^8 \frac{\text{N}}{\text{m}^2}} \right)^{1/3} = 2.2 \times 10^{-4} \text{ m}$$

The use of the modulus, E , of 275,800 MPa is conservative, since the modulus probably decreases in the same manner as the strength in Figure 1. The calculated stresses are proportional to the value of the modulus used.

The maximum tensile stress for $\sigma_0 = \frac{3P}{2\pi a^2}$ is

$$\sigma_t = \left(\frac{1 - 2\nu}{3} \right) \sigma_0 = \left(\frac{1 - 2 \times 0.02}{3} \right) \frac{3 \times 270}{2\pi (2.2 \times 10^{-4})^2} = 852 \text{ MPa}$$

so that if the pebbles behave elastically they fracture, since the tensile yield is 276 MPa.

4.4 Total Stresses

The thermal and swelling stresses are calculated for three positions in the hybrid reactor blanket: (a) near the first fuel zone wall, (b) near the center, and (c) near the back wall of the blanket. The differential swelling stress is calculated for an irradiation time of 1 year.

The internal thermal stress from thermal expansion of a solid pebble assuming steady heat flow¹³ is calculated as follows. The radial stress at the outside surface $\sigma_r = 0$, and the tangential stress

$$\sigma_t = \frac{\alpha E}{1 - \nu} \left(\frac{3}{b^3} \int_0^b T r^2 dr - T_0 \right)$$

where

$$\alpha = 17.8 \times 10^{-6} \text{ per } ^\circ\text{C}$$

See Thermal Modeling Section for thermal gradients. For a linear gradient

$$T = 407 - \frac{r}{1.5} (407 - 399) = 407 - 5.33 r$$

$$\sigma_t = \left(\frac{17.8 \times 10^{-6} \times 275,800}{0.98} \right) \left(\frac{3}{(1.5)^3} \int_0^b 407 r^2 - 5.33 r^3 dr - 399 \right) = 10 \text{ MPa}$$

$$\text{for subsequent calculations } \sigma_t = 5.009 \left(0.889 \int_0^{1.5} T r^2 dr - T_0 \right).$$

The calculations near the first zone wall will be shown.

The differential swelling stresses were first calculated with two assumptions: (a) the stresses were axisymmetric, and (b) no stress relaxation was applied until the calculation was completed (wherein a 50% relaxation was applied).

Differential swelling stresses were conservatively estimated by solving the axisymmetric problem twice and adding the absolute values of the stresses from each solution. First the gradient from the center of the sphere to the front side was used as though it were axisymmetric. Then the gradient from the center to the back side was used in the same manner. One

of these cases gave a tensile stress and the other a compressive stress. The magnitude of the two values were then added and the resulting stress was considered as tensile.

The stress on the outside surface of the sphere is of greatest importance since it is a tensile stress and beryllium is weaker in tension than in compression. It was assumed that each sphere was put back (after the two month changeout) in the exact same position with the same orientation to maximize the fluence gradient.

TABLE 4. CALCULATION FOR PEBBLE NEAR THE FIRST FUEL ZONE WALL

Position	ϕ	T_t, K	$\frac{\Delta L}{L}$	$T_o = T_s = \frac{\Delta L}{\sigma L}$	$Tr^2 dr$	σ_s (MPa)
Near front	3.2×10^{22}	673	0.0128	720	781	-129
At center	2.9	680	0.011	616	--	--
At back	2.6	672	0.0084	472	571	181

For 50% stress relaxation the differential swelling stress per year is then $\sigma_s = 0.5 (129 + 181) = 155 \text{ MPa/y}$.

The estimated life is then $\frac{276 - 10}{155 \times 0.7} = 2.4 \text{ y}$ for a 70% usage factor.

5. CONCLUSIONS

The thermal and differential swelling stresses for the three positions in the hybrid reactor blanket are tabulated (Table 5). The differential swelling stresses for the center and back wall positions were calculated for 2 and 10 years and averaged per year in Table 5. This was done, as Equation (1) is assumed to be more reliable near the fluence range at which the data for the equation were obtained, i.e., the fluence for 2 years at the center of the blanket and the fluence for 10 years at the back of the blanket. Because of the exponentials on fluence and temperature, the swelling increases very rapidly with fluence and temperature above the data range.

Other orientations of the pebble were investigated for maximum stress. When the thorium snap-ring was oriented horizontally, the side with the higher temperature and higher fluence would be in compression, while the opposite side would be in tension. The resulting thermal stress was approximately the same, e.g., 10 MPa or 3 MPa, as in Table 5, but the differential swelling stress was lower (with the same assumptions), for example, 64 MPa versus 155 MPa, or 12 MPa versus 26 MPa. Accordingly, the estimated life from Table 5 is plotted in Figure 7, from which the pebble average life in the blanket is obtained as 6.5 years.

TABLE 5. INTERNAL STRESSES FOR REFERENCE FUEL PELLETS FOR ONE YEAR IRRADIATION AND ESTIMATED LIFETIME

Position in Blanket	Stress			Estimated Lifetime (70% Usage Factor) Years
	Thermal (MPa)	Swelling (MPa/y)	Total for One Year Accumulation	
Near first wall	10	155	165	2.4
Near center	7	71	78	5.4
Near back wall	3	26	29	15.

INEL 30986

Figure 7. Estimated lifetime in years.

6. DESIGN IMPLICATIONS

The lifetime of the reference fuel-beryllium pellet was found to be superior to the hollow and solid pellets from axisymmetric calculations. However, more accurate three dimensional linear elastic stress analysis could be used to eliminate some of the conservatisms. The swelling equation could be improved with data at higher fluence, but comparison with the hollow and solid pellets would be similarly affected.

The effect of small cracks on the failure of the pebbles would need further consideration in the design and fabrication of the pellets. Considerations for increased lifetime of the beryllium balls are important to reduce the cost of replacement. There are, however, other conditions which will affect the cost such as automated fabrication and recycling of reconditioned beryllium. Factors which affect the ductility of irradiated beryllium will affect the lifetime. These factors are complex because of the sensitivity of the mechanical properties of the metal beryllium to other properties, such as fabrication, anisotropy, grain size, purity, percent BeO, etc. Generally, beryllium of fully dense low BeO content, fine grain and hot pressed, has the best anisotropic ductility. However, preferred orientation by extrusion increases the ductility in the extruded direction, so that considerations of the behavior of the beryllium in the irradiation, stress, and temperature environment may change the ductility assessment.

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