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FUSION POWER PROGRAM
QUARTERLY PROGRESS REPORT
April—June 1978

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ARGONNE NATIONAL LABORATORY, ARGONNE, ILLINOIS

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ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
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FUSION POWER PROGRAM
QUARTERLY PROGRESS REPORT

April—June 1978

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FOREWORD

This quarterly report describes fusion-related activities in research, development and reactor design, analysis and safety conducted within the Fusion Power Program and within other DOE-funded programs at Argonne National Laboratory.

The last six quarterly reports issued were:

ANL/FPP-76-6	October-December 1976
ANL/FPP-77-1	January-March 1977
ANL/FPP-77-2	April-June 1977
ANL/FPP-77-4	July-September 1977
ANL/FPP-77-7	October-December 1977
ANL/FPP-78-1	January-March 1978

ABSTRACT

This quarterly report summarizes the Argonne National Laboratory work performed for the Office of Fusion Energy during the April-June 1978 quarter in the following research and development areas: materials; energy storage and transfer; tritium containment, recovery and control; advanced reactor design; systems studies; neutronics; atomic data; reactor safety; and other work related to fusion power.

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I. FUSION REACTOR MATERIALS

A. Plasma Materials Interactions

1. Correlation of Blister Diameter with Skin Thickness for Ni and Be

S. K. Das, M. Kaminsky and G. Fenske, Physics Division

We have shown recently^{1,2} that for the case of ${}^4\text{He}^+$ irradiation of the two bcc refractory metals Nb and V, the $D_{\text{mp}} \propto t^{3/2}$ relationship does not hold. The aim of the present work was to see how the $D_{\text{mp}}-t$ relationship changes for an fcc metal, nickel, and an hcp metal, Be. For Be, EerNisse and Picraux³ suggested a $D \propto t^{3/2}$ relationship, but there was only one data point on the plot of D_{mp} vs. t . To the author's knowledge, there is no data available on the $D-t$ relationship for Ni.

Figures I-1(a) and I-1(b) show plots of projected ranges of ${}^4\text{He}^+$ ions in Ni and Be, respectively, for different energies. The solid curves in Figure I-1 show projected ranges for ${}^4\text{He}^+$ on nickel and for beryllium, respectively, for different energies calculated according to Brice⁴ which uses Thomas-Fermi nuclear stopping cross sections together with semiempirical values for electronic stopping powers. The dotted curves in Figure I-1 were taken from the recent calculations published by Ziegler.⁵

The blister skin thickness values for nickel irradiated at room temperature with ${}^4\text{He}^+$ ions having energies ranging from 20 to 500-keV are plotted in Figure I-1(a). In this case, the blister skin thickness values for energies of 80 keV and above agree within $\sim 10\%$ with the calculated projected range, whereas for energies < 60 keV the values are higher than the calculated projected range. For an energy of 20 keV the blister skin thickness is almost twice the calculated projected range. This trend observed for nickel is very similar to that observed for ${}^4\text{He}^+$ irradiation of niobium.⁶

The experimentally measured blister skin thickness values for ${}^4\text{He}^+$ irradiated Be for ion energies from 10 to 100-keV are also plotted in Figure I-1(b). It may be noted that for the energy range 15 to 40-keV,

¹ M. Kaminsky and S. K. Das, Fusion Power Program Quarterly Report, Argonne National Laboratory, ANL/FPP-78-1 (1978).

² S. K. Das, M. Kaminsky and G. Fenske, Fusion Power Program Quarterly Progress Report, Argonne National Laboratory, ANL/FPP-77-2 (1977).

³ E. P. EerNisse and S. T. Picraux, J. Appl. Phys. 48 (1977) 9.

⁴ D. K. Brice, Ion Implantation Range and Energy Deposition Distribution, Vol 1 (Plenum, New York, 1975).

⁵ J. F. Ziegler, Helium: Stopping Power and Ranges in All Elements, Vol. 4 (Pergamon, New York 1977).

⁶ R. G. St.-Jacques, J. G. Martel, B. Terreault, G. Vielleux, S. K. Das, M. Kaminsky and G. Fenske, J. Nucl. Mat. 63 (1976) 273.

Figure I-1. Projected ranges of $^4\text{He}^+$ ions (a) in nickel and (b) in beryllium as a function of projectile energy. The solid curve marked "Brice" was calculated using Thomas-Fermi nuclear stopping and Brice's semi-empirical expression for electronic stopping. The dotted curve was taken from the range-energy curves calculated by Ziegler. The data points with error bars are measured skin thickness values for annealed polycrystalline metals irradiated at room temperature with $^4\text{He}^+$ ions to total doses ranging from 0.1 to 0.5 C/cm².

the skin thickness values agree within $\sim 10\%$ with the calculated projected range. For energies below 15 keV the skin thickness values are higher than the calculated projected range, whereas for energies of 60 keV or higher, they are lower than the projected range calculated according to Brice,⁴ but agree well with those calculated by Ziegler.⁵

The blister diameters were analyzed from a large number of micrographs in order to obtain better statistics. Table I-1 summarizes a typical data set for annealed polycrystalline nickel. Here, the number of blisters analyzed for each energy are listed together with the values for minimum ($D_{\min}$), maximum ($D_{\max}$), most probably (D_{mp}) and average (D_{av}) blister diameter. The standard deviation of the blister diameter distribution for each energy is also listed in Table I-1. In the last two columns the values for experimentally measured blister skin thicknesses are compared with the projected ranges calculated according to Brice.⁴ For a ${}^4\text{He}^+$ ion energy of 500 keV, the blister skin thickness value is considered reliable while the blister diameter (e.g. D_{mp} and D_{av}) values are not considered to be reliable because one observed only a few large blisters over the entire bombarded area. For energies below 20 keV we feel that there are large uncertainties in the values of D and t .

Figure I-2 shows a double logarithmic plot of blister diameter vs. blister skin thickness for annealed polycrystalline nickel irradiated at room temperature with ${}^4\text{He}^+$ ions. The ranges of blister diameters observed at a given projectile energy, i.e., for a given mean blister skin thickness, are shown by the vertical bars and the ranges of skin thickness are shown by the horizontal bars in Figure I-2. The values of most probably D_{mp} and the average blister diameter D_{av} , given in Table I-1 are also plotted in Figure I-2 for a given mean blister skin thickness. The solid line is a power relation fit of D_{mp} with mean blister skin thickness, t , and it gives the relationship $D_{\text{mp}} = 12.4 t^{1.15}$, where D_{mp} and t are given in units of micrometers. The curve fitting was done in the manner described earlier^{1,2} for Nb, and the coefficient of determination r^2 , which indicates the quality of the fit (a value of r^2 closer to unity indicates a better fit than a value closer to zero) for the above fit was 0.93. A power relation fit for the average diameters, as plotted in Figure I-2, with the mean blister skin thickness, t , gave the relationship $D_{\text{av}} = 16.2 t^{1.39}$ (with $r^2 = 0.96$), and for the minimum blister diameter $D_{\min}$ with t gave the relationship $D_{\min} = 10.74 t^{1.82}$ (with $r^2 = 0.94$).

Figure I-3 shows a double logarithmic plot of blister diameter against skin thickness for annealed polycrystalline beryllium irradiated at room temperature with ${}^4\text{He}^+$ ions. Here, also, the vertical bars represent the range of blister diameters observed at a particular mean blister skin thickness, whose error limits are shown by the horizontal bars. Also plotted in Figure I-3 are the values for the most probable and average blister diameters. A power curve fit of D_{mp} with t gives the relationship $D_{\text{mp}} = 24.6 t^{1.25}$, which is shown as the solid line in Figure I-3. The fit is quite good with $r^2 = 0.99$. Power curve fits for D_{av} with t and $D_{\min}$ with t , give the relationship $D_{\text{av}} = 31.6 t^{1.4}$ (with $r^2 = 0.96$) and $D_{\min} = 16.4 t^{1.57}$ (with $r^2 = 0.88$), respectively.

Table I-1. Blister Diameter and Blister Skin Thickness for Annealed Polycrystalline Ni Irradiated at Room Temperature with $^4\text{He}^+$ Ions of Various Energies

$^4\text{He}^+$ ion energy (keV)	No. of Blisters Counted	BLISTER DIAMETER (μm)					Blister Skin Thickness, t (μm)	Projected Range Calculated According to Brice, (μm)
		Min Dia. $D_{\min}$	Max. Dia. $D_{\max}$	Avg. Dia. D_{av}	Most Probable Dia, D_{mp}	Standard Deviation		
20	515	0.22	1.25	0.91	0.99	0.18	0.13 ± 0.03	0.0666
30	435	0.36	1.95	1.11	1.45	0.35	0.17 ± 0.02	0.1006
40	762	0.41	2.47	1.50	1.73	0.39	0.19 ± 0.02	0.1336
60	413	1.14	3.90	2.62	3.07	0.58	0.25 ± 0.03	0.1955
80	541	1.44	4.40	3.18	3.28	0.45	0.27 ± 0.02	0.2527
100	582	1.52	5.90	3.82	4.18	0.74	0.30 ± 0.02	0.3060
150	464	3.30	11.10	5.13	4.90	1.14	0.50 ± 0.04	0.4256
300	384	4.76	27.70	10.74	8.0	4.10	0.77 ± 0.1 0.06	0.7207
500	---	---	---	---	---		1.1 ± 0.05	1.0283

Figure I-2. A Double logarithmic plot of blister diameter against blister skin thickness for annealed polycrystalline nickel irradiated at room temperature with $^4\text{He}^+$ ions. The error bars show the ranges of values observed at a given $^4\text{He}^+$ ion energy.

Figure I-3. A double logarithmic plot of blister diameter against blister skin thickness for annealed polycrystalline beryllium irradiated at room temperature with ${}^4\text{He}^+$ ions.

The results presented here for a fcc metal, Ni and a hcp metal, Be, together with those we have reported for bcc metals, V and Nb, indicate that the relationship $D_{mp} \propto t^{3/2}$, predicted by the lateral stress model does not hold. According to the lateral stress model³ the most probably blister diameter, D_{mp} can be written as

$$D_{mp} = \left[\frac{K E}{2.5 \times 10^{-4} (1-p^2) \sigma_y} \right]^{1/2} t^{3/2} \quad (1)$$

where E is Young's modulus, p is Poisson's ratio, σ_y is the yield strength t is blister skin thickness and K is a geometric factor which ranges from 1.4 to 4.9 for elastic edge conditions ranging from a simply supported edge to a clamped edge, respectively. EerNisse and Picraux³ claimed that the D-t relationship predicted by equation (1) agreed within 40% with the experimental data for Nb, V, Be, Mo, Ti, Pd and 4301 stainless steel. However, as pointed out earlier, for metals such as Be and V there were only one or two data points on which the correlation was based. For the case of Nb the authors have shown earlier¹⁻² that the data used for the $D_{mp} \propto t^{3/2}$ correlation over a broad range of $^4\text{He}^+$ ion energies may have suffered from poor statistics for high energies (> 1 MeV) and there may be large errors in the values of D_{mp} and t for energies below 15 keV, since they depend strongly on the total dose at these low energies.

Table I-2 compares our experimental D_{mp} - t relationships for Be, V, Ni and Nb with those calculated from equation (1) for the two extreme values of K using the values of E, p, and σ_y from the literature.⁷⁻⁸ One should notice that these values are characteristic for annealed metals and may differ from those for irradiated metals. It can be seen that the agreement between the experiment and the predictions from the stress model is far from satisfactory. Even for the extreme case of a simply supported edge ($K = 1.4$) the pre-exponent factor in the D_{mp} - t relationship predicted by the stress model is much larger (by almost a factor of 2-4 for V, Ni, Nb) than the experimental value. Furthermore, the stress model predicts the exponent of t to be independent of material, whereas the experimental values show them to be strongly dependent on the type of metal (e.g. value of exponent varies from 0.85 for V to 1.25 for Be) studied.

In summary, the relationship between the most probable blister diameter, D_{mp} , and the mean blister skin thickness, t, for nickel held at room temperature, can be expressed as $D_{mp} = 12.4 t^{1.15}$, and for annealed polycrystalline beryllium, can be expressed as $D_{mp} = 24.6 t^{1.25}$. These relationships, together with earlier results obtained for Nb and V, show that the relationship between D_{mp} and t is dependent on the type of metal studied and does not support the lateral stress model for blister formation which predicts $D \propto t^{1.5}$ for many metals, including Be, Nb and V.

⁷ C. R. Tipton, Jr., editor, Reactor Handbook, Vol. 1 (Interscience Publishers, New York, 1960).

⁸ Metals Handbook, Vol. 1 (American Soc. for Metals, Metals Park, Ohio, 1961).

Table I-2. Power Relationships for Most Probable Blister Diameter, D_{mp} (μm) and Mean Blister Skin Thickness, t (μm) as Predicted by the Lateral Stress Model and as Obtained Experimentally for Different Metals.

Metal (annealed poly- crystalline)	Young's Modulus E ($\times 10^{12}$ dynes/cm ²)	Poisson's Ratio, p	Yield Strength σ_y ($\times 10^9$ dynes/cm ²)	Relationship predicted by stress model*		Experimental Relationship
				for k = 4.9	for k = 1.4	
Be	2.75	0.027	1.18	$D_{mp} = 54.7t^{1.5}$	$D_{mp} = 29.2t^{1.5}$	$D_{mp} = 24.6t^{1.25}$
V	1.38	0.35	1.17	$D_{mp} = 51.3t^{1.5}$	$D_{mp} = 27.4t^{1.5}$	$D_{mp} = 6.3t^{0.85}$
Ni	2.06	0.276	0.58	$D_{mp} = 86.8t^{1.5}$	$D_{mp} = 46.4t^{1.5}$	$D_{mp} = 12.4t^{1.15}$
Nb	1.03	0.39	1.45	$D_{mp} = 40.0t^{1.5}$	$D_{mp} = 21.4t^{1.5}$	$D_{mp} = 10.3t^{1.22}$

* Calculated using the expression $D_{mp} = \left[\frac{K E}{2.5 \times 10^{-4} (1-p^2) \sigma_y} \right]^{1/2} t^{3/2}$ given by lateral stress model.³

2. Measurements of Sputtering Yields for Polycrystalline Mo Under D⁺ Ion Irradiation

M. Kaminsky and P. Dusza, Physics Division.

The total sputtering yields for polycrystalline Mo (a candidate material for armor plates for TFTR) have been measured for 40-, 60- and 120-keV D⁺ irradiation. These energies were chosen because the armor plates in TFTR will experience interaction with D⁺ ions at these energies.¹

For measurements of the total sputtering yield S a monocrystalline Si (111) collector was placed at a distance of 4.75 mm from the surface of the target which was bombarded by the incident beam. The collector had a 3 mm hole to pass the beam through. A tantalum aperture with a 2 mm hole was placed in front of the collector side facing the incident beam. Another aperture was placed between the collector and the target which was biased (-100 V) with respect to the target for secondary electron suppression. This aperture had a sufficiently large slot to allow the sputtered material to pass through and to be deposited on the collector.

For a determination of the total sputtering yield S it is necessary to determine the total mass M which has been emitted from the irradiated target spot. Since the sputtered Mo is emitted with an angular distribution which appears to follow to a good approximation a cosine distribution, the deposit thickness will not be uniform over the deposit area but will follow to a good approximation a cosine distribution, too. For a target spot of radius r, a distance l between the center of the deposit and the center of the target spot, a distance R measured from the center of the deposit along a radial direction of the deposit spot, the deposited material density ρ, and the deposit thickness t, the emitted mass M is given by

$$M = \frac{2 \pi \rho r^2 t}{\left(1 - \frac{1 + \left(\frac{R}{l}\right)^2 - \left(\frac{r}{l}\right)^2}{\sqrt{\left[1 - \left(\frac{R}{l}\right)^2 + \left(\frac{r}{l}\right)^2\right] + 4 \left(\frac{R}{l}\right)^2}} \right)} \quad (1)$$

For a determination of (a) a center line of the deposit spot, and (b) of the fraction t(R), the Rutherford backscattering technique was used with a 750-keV ⁴He⁺ beam, a beam diameter of 0.3 μm (note, ~ 99% of the Mo deposit was in a circular spot with a diameter of ~ 20 mm), and a scattering angle (lab. syst.) of 154°.

Figure I-4 illustrates a Rutherford backscattering spectrum from a 4.5 monolayer (ML) thick molybdenum deposit (average thickness) on a Si (111) substrate obtained by the scattering of 750-keV ⁴He⁺ ions under Lab angle of 154°. One can readily distinguish the Mo peak from the broad Si spectrum.

¹ S. K. Das, M. Kaminsky, P. Dusza, Fusion Power Program Quarterly Progress Report, ANL/FPP-77-4; also, J. Vac. Sci. & Technology 15, 710 (1978).

Figure I-4. Rutherford backscattering spectrum of a ~ 4.5 monolayer thick Mo deposit on a Si(III) substrate (resulting from a 60-keV D^+ Mo sputtering run) obtained with a 750-keV primary $^4\text{He}^+$ ion beam and for a scattering angle of 154° .

Figure I-5. Variation of the Mo deposit thickness t as a function of distance R on the substrate as measured from the center of the deposit spot ($R=0$) along radial directions. The solid curve is based on calculated values using equation (1). The experimentally determined values are indicated by +.

Figure I-6. The dependence of the sputtering yield S on the incident ion energy for polycrystalline molybdenum targets bombarded by deuterons at normal incidence. Experimental values marked (Δ) obtained by Bay et al [KAP 48, 4722 (1977)], and those marked ($\circ$) see this report. The solid line has been interpolated between experimental values. The dotted curve has been calculated according to Sigmund's theory using appropriate corrections for backscattering, electronic stopping, and surface reflection.

The area A under the Mo peak is a measure of the deposit thickness t. A quantitative correlation between A and t (in units of ML) was obtained by calibration standards. These standards had been prepared by vapor deposition of known amounts of Mo on a Si (111) substrate. The thickness of such deposits was measured independently by the Rutherford backscattering technique and by depth profiling with Auger spectrometry. The Rutherford backscattering technique allowed measurements of a Mo deposit thickness as small as 0.001 ML.

Figure I-5 shows the function t (R) vs R as calculated from equation (1), and the experimentally determined values of t (R) for Mo sputtered by 60-keV D⁺ ions have been included (see points marked †). The agreement between calculated and experimental values appears to be good.

In these studies we determined the total sputtering yields, S for 40-, 60- and 120-keV D⁺ ion bombardment of polycrystalline Mo surfaces at normal incidence. In the order of the energy values listed above we obtained $S = (1.10 \pm 0.11) \times 10^{-3}$ atom/ion, $S = (0.75 \pm 0.07) \times 10^{-3}$ atoms/ion, and $S = (0.4 \pm 0.05) \times 10^{-3}$ atoms/ion. In Figure I-6 we have plotted these yield values as a function of deuteron energy together with S- values obtained previously by Bay et al.² for the same ion-target system but at lower deuteron energies. The dotted curve has been calculated according to Sigmund's theory³ using appropriate corrections for backscattering electronic stopping, and the surface reflection as suggested by Weissman, et al.⁴ and by Anderson, et al.⁵ and by normalizing a calculated value with the experimental value at 2 keV. In general, the calculated curve seems to represent the energy dependence of the yield value quite well for the energy range shown here, although relatively small deviations at lower and higher energies can be observed. However, without fitting the calculated curve to an experimental value near $S_{\max}$, the calculated values would be a factor 6.5 to 8 larger.

At this time we have only some very tentative indications that S-values for light ion bombardment (e.g. H⁺, D⁺, He⁺) depend on the total dose. If we can substantiate this in future studies, then the yield values reported here should be considered as being typical only for the dose range studied. In the summary, we have already pointed out that based on the yield values reported here, the thickness loss of Mo "armored plates" due to sputtering by 40-, 60-, and 120-keV D⁺ ions would be negligibly small (≈ 150 Å for a long period of 10,000 deuterium pulses and for the deuterium fluxes estimated. However, during a 1 s deuterium pulse an average Mo impurity concentration $n_i = 1.5 \times 10^{10}$ atom cm⁻³ could be built up in a TFTR plasma, resulting in a $Z_{\text{eff}} = 1.31$. This in turn will cause an increase in the plasma radiation losses (e.g., a 32% increase in the bremsstrahlung losses of a hydrogenic plasma, with plasma density of 1×10^{14} atoms/cm³, if the Mo impurity atoms are fully stripped).

² H. L. Bay, et al., J. Appl. Phys. 48, 4722 (1977).

³ P. Sigmund, Phys. Rev. 184, 383 (1969).

⁴ R. Weissman and P. Sigmund, Rad. Effects 19, 69 (1973).

⁵ H. H. Anderson, et al., J. Appl. Phys. 46, 2416 (1975).

3. Deuterium Trapping in Ion-Bombarded Beryllium

Ming-Bian Liu, Irving Sheft and Dieter M. Gruen, Chemistry Division

Radiative power losses from a fusion plasma due to impurity ions increase dramatically with the atomic number of the impurity. Beryllium, a low-Z element, is thus a potential candidate first-wall material or coating for minimizing such losses. Trapping and retention of deuterium and particularly of tritium in beryllium are important parameters which need to be established before potential applications of this material in a fusion device can be contemplated. Earlier studies have shown that trapping of deuterons in titanium, a metal which forms the stable, deuteride TiD_2 , proceeds with an efficiency close to 100% up to fluences of $\sim 10^{19} D^+/cm^2$.^{1,2} Similar studies have now been carried out on beryllium with an experimental apparatus which has been described previously.¹ Two beryllium disks (19 mm diameter; 0.79 mm thick) were outgassed in a vacuum of 10^{-8} torr at $1100^\circ C$ for 1/2 hour. After cooling, the disks were mounted in air on a target holder and returned to the vacuum chamber, which after pumpdown to 10^{-8} torr was baked at $\sim 200^\circ C$ for 3-4 hours. The disks were bombarded with $63 \mu A$ of 10 keV D^+ normal to the surface direction. Irradiation was terminated at a fluence of $1.5 \times 10^{19} D^+/cm^2$.

Indium gaskets, used to provide thermal contact to the holder showed no sign of melting after the experiment indicating that the target temperature was below $150^\circ C$, the m.p. of indium, during the bombardment. One of the bombarded disks was mounted on an energy dispersive X-ray spectrometer³ for examination, while the other was transferred to a tensimetric apparatus,¹ where the trapped deuterium, released by heating in vacuum up to $500^\circ C$, was measured as previously described.¹ Duplicate experiments were done with disks outgassed at $500^\circ C$ for 4 hours before irradiation and kept in a nitrogen atmosphere with minimal exposure to air before being transferred from the bombardment chamber to the tensimetric analysis system. The gas collected in the tensimetric apparatus during the heating of the bombarded disks was mass-spectrometrically analyzed.

Experimental results are summarized in Table I-3. Column 1 identifies the experiment, column 2 indicates the condition under which the disks were handled after bombardment, columns 3 and 4 list the fluence of D^+ , columns 5 and 6 give the amount and composition of gas collected as a result of heating the bombarded disk in the tensimetric apparatus, and column 7 calculates the trapping efficiency of D^+ in beryllium. The measured trapping efficiencies of D^+ in beryllium were comparable in the two experiments although the amounts of gas collected under different handling conditions were very different. The main difference in the results for the two measurements

¹ I. Sheft, H. Reis, Jr., D. M. Gruen and W. Peterson, J. Nucl. Mater. 59 (1976) p. 1.

² J. Bohdansky, J. Roth, M. K. Sinha, and W. Ottenberger, Jr., J. Nucl. Mater. 63 (1976) p. 115.

³ M. B. Liu, D. M. Gruen, A. R. Krauss, A. H. Reis, Jr., and S. W. Peterson, High Temp. Sci. (in press).

Table I-3. Trapping Efficiency Measurements of 10 keV D⁺ in Beryllium Metal

Experiment Number	Condition	Fluence		Gas Collected		Trapping Efficiency Percent
		D ⁺ /cm ²	μmole	μmole	Composition	
A	Bombarded disk exposed to air for 7 days	1.50 x 10 ¹⁹	5.49	5.06	13.4% H ₂ , 1.0% HD, 2.8% D ₂ 1.2% CH ₄ , 70.4% H ₂ O, 0.9% HDO 3.4% N ₂ , 0.4% O ₂ , 1.5% CO ₂ , and 5.0% hydrocarbons	3.5
B	Bombarded disk kept in N ₂ atmosphere with less than 5 min. exposure to air	1.43 x 10 ¹⁹	5.23	1.23	14.0% H , 1.7% HD, 8.2% D , 71.4% H ₂ O, 2.5% N ₂ , CO, hydro- hydrocarbons, and 2.2% CO ₂	

was in the amount of H_2O collected by heating the bombarded disk: 3.56 μmole in Exp. A., 0.878 μmole in Exp. B. The source of the H_2O most likely was the ambient environment since the bombarded disk in Exp. A was exposed to air for 7 days before analysis was performed while the bombarded disk in Exp. B was not exposed to air longer than 5 minutes. The amounts of H_2O collected by heating the bombarded disks represent 757 monolayers (Exp. A) and 93 monolayers (Exp. B), respectively. Exposure of unbombarded beryllium to air at room temperature on the other hand has been shown to results in films of nonporous protective oxide about 100 Å thick in 2 hours.⁴ These results strongly indicate that deuteron bombardment of beryllium creates a hygroscopic surface reminiscent of the behavior of deuteron bombarded aluminum oxide.⁵ Table I-3 shows that only small amounts of HD and HDO species were found, implying that the adsorbed water undergoes virtually no exchange with the trapped deuterium.

X-ray examination of one of the bombarded disks showed that no new crystalline phases were formed as a result of deuterium irradiation. Nonetheless, it is possible that beryllium deuteride formed in an amorphous state. No direct reaction between beryllium and hydrogen or deuterium has ever been reported, although a "beryllium hydride" can be prepared through an organic pyrolysis⁶⁻⁸ procedure. X-ray examination of such pyrolysis products are inconclusive and no beryllium hydride with well defined properties appears to be known. Although it cannot be definitely concluded that beryllium has not reacted chemically with the implanted D^+ , the lack of X-ray evidence for reaction and the instability of beryllium deuteride combine to favor such an interpretation.

The average amount of deuterium in the bombarded beryllium disks was $0.15 \pm 0.04 \mu\text{mole}$ of D_2 which works out to $4 \times 10^{17} \text{ D}^+/\text{cm}^2$ with a bombarded area of 0.442 cm^2 . It is of interest to compare the amount of trapped D_2 with the solubility of hydrogen or deuterium in beryllium. The implanted atoms can extend to a depth of $R_p + (\Delta R_p^2 + 2Dt)^{1/2}$,¹⁰ where R_p is the projected range, ΔR_p is the standard deviation of projected range and D and t are the

⁴ R. O. Adams and J. T. Hurd, J. Less-Common Metals, 18 (1969) p. 399.

⁵ D. M. Gruen, B. Siskind and R. B. Wright, J. Chem. Phys., 65 (1976) p. 363.

⁶ E. L. Head, C. E. Holley, Jr., and S. W. Rabideau, J. Amer. Chem. Soc. 79 (1957) 3687.

⁷ L. Banford and G. E. Coates, J. Chem. Soc. (1964) p. 5591.

⁸ E. C. Ashby and H. S. Prasad, Inorg. Chem. 14 (1975), p. 2869.

⁹ A value for the room temperature hydrogen isotope retention in BeO/Be of $\sim 1 \times 10^{17} \text{ atom cm}^{-2}$ for 1 keV D^+ bombardments has recently been listed in Table I of "Surface Processes in Plasma Wall Interactions" W. Bauer, SAND-78-8634, April 1978.

¹⁰ G. Dearnaley, J. H. Freeman, R. S. Nelson and J. Stephen, "Ion Implantation", American Elsevier Publishing Co., Inc., New York, 1973.

diffusion constant and the time, respectively. With a projected range of 10 keV D^+ in beryllium of 2×10^{-5} cm,¹¹ a diffusion coefficient of hydrogen (deuterium) in beryllium at 150°C of 1.5×10^{-9} cm²/s (upper limit),¹² a total bombarding time of 1.7×10^4 s, and making the reasonable assumption that ΔR_p is equal to R_p , one calculates that the implanted deuterium atoms might extend to a depth of as much as 7.1×10^{-3} cm. The solubility of hydrogen (deuterium) in beryllium is less than 0.00075 at %.¹³ Using these data, one estimates the maximum amount of deuterium in the beryllium target limited by solubility considerations to be 2.4×10^{-3} μ mole which is two orders of magnitude smaller than the measured amount.

In view of the small solubility of D_2 in Be and in the absence of evidence for the formation of bulk beryllium deuteride, one must find an alternative explanation for the trapping of deuterium in amounts far exceeding its solubility limit. One possible mechanism involves blister formation and retention of deuterium in blisters.

Verbeek and Eckstein¹⁴ studied the radiation blistering of beryllium as a result of 15 keV deuteron implantation. They report the onset of blister formation at fluences below 3×10^{17} D^+ /cm² with most of the blisters having cracked at fluences of 10^{18} D^+ /cm². At fluences of 1.5×10^{19} D^+ /cm² typical of the present experiments, blisters could form, crack and possibly reform until a steady state concentration of blisters is reached limited by a critical fluence. The critical fluence for 10 keV deuterons on Be may be estimated from that found for 15 keV deuterons, if one assumes that it is proportional to the blister diameter, d_b . Kaminsky, et al.¹⁵ found that in the case of helium blistering of Be, the skin thickness, Δ , agrees to within $\sim 10\%$ with the calculated projected range, R , and $d_b = 24.6 \Delta^{1.25}$. If the above correlation can also be applied to deuterium blistering of Be, the diameter of the blisters will be approximately proportional to $R^{1.25}$. Therefore, the critical fluence for 10 keV deuterons is estimated from the projected range¹¹ to be 2.3×10^{17} ions/cm², in reasonable agreement with the value 4×10^{17} ions/cm² calculated from the measured amount of trapped deuterium.

The hypothesis that a steady state concentration of intact blisters is established on D^+ bombarded beryllium surfaces, essentially independent of fluence, still remains to be tested. In view of the large amount of trapped D_2 in Be, explanations other than retention of D_2 in blisters do not readily come to hand.

¹¹H. H. Anderson and J. F. Ziegler, Hydrogen Stopping Powers and Ranges in All Elements, Pergamon Press, Inc., New York, 1977.

¹²P. M. S. Jones and R. Gibson, J. Nucl. Mater. 21 (1967) 353.

¹³J. P. Pemsler and E. J. Rapperport, Trans. Metallurg. Soc. of AIME, 230 (1964) 90.

¹⁴H. Verbeek and W. Eckstein, in "Applications of Ion Beams to Metals", p. 597, S. T. Picarux, E. P. EerNisse and F. L. Vook, Eds. Plenum Press, New York (1974).

¹⁵S. K. Das, M. Kaminsky and G. Fenske, J. Appl. Phys. (submitted).

B. Dosimetry and Damage Analysis

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1. Integral Cross-Section Tests at ORIC

Integral neutron activation measurements have been completed for 14 materials (44 reactions) in the 40 MeV, $^9\text{Be}(d,n)$ field at ORIC. Foils were irradiated in a well-defined geometry at 7.62 cm from the neutron source at angles of 0° and 15° to the beam direction. Available cross-sections have been extrapolated to 44 MeV for 29 of the reactions. The neutron spectra at various angles have been previously measured by M. Saltmarsh, et al¹ using the time-of-flight technique. Our activation measurements are compared to activities calculated using the time-of-flight spectra and our extended cross-section library in Table I-4. As can be seen, the overall agreement of about $\pm 10\%$ is surprisingly good.

The second column indicates the 90% energy limits for the calculated activities. The good agreement between measured and calculated results comes mainly from the fact that we are for the most part testing the cross-sections at energies where they are better known. The large discrepancies for the (n,p) reactions on ^{46}Ti , ^{47}Ti , and ^{54}Fe are due to contributions from higher-mass isotopes. The numbers in parenthesis show that these effects are very nearly accounted for using the THRESH code of S. Pearlstein.²

Figures I-7 and I-8 compare the spectra at 0° and 15° (unfolded with the SAND II code) with the time-of-flight measurements. The slight differences are generally within the experimental accuracies of both techniques.

The excellent results obtained in a well-defined geometry indicate that spectral analysis in a poorly defined geometry should be reasonably reliable. The SAND II code was thus used to unfold the flux-spectrum obtained from thin Au, Ni, Co, and Fe foils placed directly on the tantalum end-plate approximately 6.25 mm from the neutron source. The foils were placed in a cross pattern and cut into 14 subsections (typically 6.2 mm square) to map flux and spectral gradients. Eleven reactions, which showed excellent agreement in Table I-4, were used to unfold the flux as a function of radial distance from the beam axis, as shown in Figure I-9. The agreement between foils located in all four directions normal to the beam axis indicates that the field is cylindrically symmetric and that the unfolding technique is quite reproducible.

An unfolded spectrum obtained from the centermost group of foils is shown with an error analysis in Figure I-10. This spectrum and Figure I-9 are of particular interest since they represent the maximum flux obtainable at ORIC for materials irradiations (although our average beam current of $13.5 \mu\text{A}$ was lower than normal).

¹M. J. Saltmarsh, C. A. Ludemann, C. B. Fulmer, and R. C. Styles, "Characteristics of an Intense Neutron Source Based on the $d + \text{Be}$ Reaction," Oak Ridge National Laboratory Report ORNL/TM-5696 (1976).

²S. Pearlstein, "Neutron-Induced Reactions in Medium Mass Nuclei," Brookhaven National Laboratory Report, BNL-16271 (1971).

Figure I-7. Comparison of time-of-flight (input) spectrum at 40 MeV at 0° and unfolded spectrum from SAND II using our integral activities.

Figure I-8. Comparison of time-of-flight (input) spectrum at 40 MeV at 15° and unfolded spectrum from SAND II using our integral activities.

Table I-4. Ratios of Measured-to-Calculated Activities ($\sigma\phi$) Using Time-of-flight Spectra. Relative errors are $\pm 1.5\%$ and absolute $\pm 10\%$, except as noted.

Reaction	90% Energy ^a Range (MeV)	Ratio at 0° ($\sim 3.5^\circ$)	Ratio at 15° ($\sim 17^\circ$)
	(0° Spectrum)		
$^{45}\text{Sc}(n,\gamma)^{46}\text{Sc}$	7.6^{-3} -25.0	1.00 ^b	1.17 ^b
$^{59}\text{Co}(n,\gamma)^{60}\text{Co}$	1.6^{-4} -22.7	$1.26^b \pm 10\%$	$1.35^b \pm 10\%$
$^{197}\text{Au}(n,\gamma)^{198}\text{Au}$	2.5^{-4} -19.5	1.03 ^b	1.10 ^b
$^{238}\text{U}(n,\gamma)^{239}\text{Np}$	4.3^{-4} - 4.2	1.02 ^b	0.88 ^b
$^{235}\text{U}(n,f)$	0.6 - 29.7	0.99 ^c	0.94 ^c
$^{238}\text{U}(n,f)$	5.0 - 30.8	1.00 ^c	0.94
$^{115}\text{In}(n,n')^{115\text{m}}\text{In}$	1.8 - 23.1	1.04	0.96
$\text{Ti}(n,p)^{46}\text{Sc}$	9.0 - 33.0	*1.84 (0.83) ^d	*1.93 (0.88) ^d
$\text{Ti}(n,p)^{47}\text{Sc}$	12.0 - 33.0	*8.82 (1.18) ^d	*7.14 (1.13) ^d
$^{48}\text{Ti}(n,p)^{48}\text{Sc}$	9.9 - 26.6	0.97	0.99
$\text{Fe}(n,p)^{54}\text{Mn}$	6.0 - 33.0	*1.89 (0.94) ^d	*1.88 (1.04) ^d
$^{56}\text{Fe}(n,p)^{56}\text{Mn}$	8.6 - 23.5	1.05	1.02
$^{59}\text{Co}(n,p)^{59}\text{Fe}$	8.1 - 24.2	$0.85 \pm 15\%$	$0.95 \pm 15\%$
$^{58}\text{Ni}(n,p)^{58}\text{Co}$	4.4 - 23.4	0.93	0.88
$^{60}\text{Ni}(n,p)^{60}\text{Co}$	7.9 - 23.0	$0.97 \pm 5\%$	$0.98 \pm 7\%$
$^{27}\text{Al}(n,\alpha)^{24}\text{Na}$	9.0 - 21.5	1.02	0.96
$^{54}\text{Fe}(n,\alpha)^{51}\text{Cr}$	9.5 - 28.0	*1.28	*1.43
$^{59}\text{Co}(n,\alpha)^{56}\text{Mn}$	9.3 - 24.4	1.05	1.02
$^{45}\text{Sc}(n,2n)^{44\text{m}}\text{Sc}$	13.9 - 27.5	0.95	0.98
$^{59}\text{Co}(n,2n)^{58}\text{Co}$	12.8 - 26.6	1.06	1.04
$^{58}\text{Ni}(n,2n)^{57}\text{Ni}$	14.8 - 28.2	$0.82 (1.26)^e$	$0.84 (1.34)^e$
$\text{Zr}(n,2n)^{89}\text{Zr}$	14.1 - 28.1	0.99	1.02
$^{93}\text{Nb}(n,2n)^{92\text{m}}\text{Nb}$	11.2 - 22.0	0.93	0.94
$^{169}\text{Tm}(n,2n)^{168}\text{Tm}$	10.4 - 23.3	0.91	0.92
$^{169}\text{Tm}(n,3n)^{167}\text{Tm}$	17.9 - 30.6	1.05	1.08
$^{197}\text{Au}(n,2n)^{196}\text{Au}$	10.7 - 23.5	0.99	0.98
$^{197}\text{Au}(n,3n)^{195}\text{Au}$	18.0 - 29.4	$0.87 \pm 4\%$	$1.06 \pm 7\%$
$^{197}\text{Au}(n,4n)^{194}\text{Au}$	27.2 - 39.8	$1.03 \pm 10\%$	$0.88 \pm 13\%$
$^{238}\text{U}(n,2n)^{237}\text{U}$	7.7 - 16.4	1.21	1.10
Std. Dev. (%)		*9.7	*10.8
Total Flux (n/cm ² -s)		6.26×10^{10}	3.21×10^{10}

* Reactions not included in standard deviation.

^a 90% of the activation integral falls within this energy range. 7.6^{-3} means 7.6×10^{-3} . The range at 15° is only slightly changed.

^b (n, γ) ratios are somewhat arbitrary since the time-of-flight data stops at 2 MeV. A smooth extrapolation was chosen to give a reasonable fit to the data.

^c 14 MeV fission yields were used.

^d Ratios not in parentheses were calculated assuming mono-isotopic production [e.g. $^{54}\text{Fe}(n,p)$]; ratios in parenthesis include production from higher mass isotopes based on THRESH calculations. Energy limits are for total production.

^e Cross-section from ENDF/B-IV and LASL; value in parenthesis from ENDF/B-IV only.

Figure I-9. Integral radial flux gradients at 6.3 mm from the source unfolded with SAND II using our integral activity measurements. Various lower energy thresholds are indicated and errors were calculated with the Monte Carlo code SANDANL.

Figure I-10. Unfolded spectrum with error analysis at closest available geometry at ORIC. This position offers the maximum possible flux for materials irradiations.

The errors shown in Figure I-10 indicate that the flux-spectrum is reasonably well-defined between 2 and 30 MeV where most of the materials damage originates. In a pure 0° spectrum, about 14% of the flux lies below 2 MeV, 9% is above 25 MeV, and only 3.5% is above 30 MeV. Hence, the larger flux errors at very low and very high energies are relatively unimportant in terms of neutron damage in materials.

A draft of our complete study is now being readied for submission to Nuclear Technology and preprints should be available shortly.

2. Fission Reactor Dosimetry

The first irradiation in ORR, labeled ORR-MFE1, is scheduled to end prematurely in late June or early July. Due to the reduced fluence it is not certain that the activation will be adequate in all materials and the HAFM's may not have sufficient helium for reliable counting at Atomic International (AI). Selected samples will thus be counted to evaluate the extent of recoverable dosimetry information.

The low-power spectral study is scheduled for an irradiation late in the fourth quarter of FY-1978. The dummy capsule will be inserted in the same location to be used for ORR-MFE2. ANL personnel will unload the capsules at Oak Ridge (ORNL) and ship the samples to Argonne for counting within a few days after the end of irradiation in time to measure the activities with short half-lives.

Plans have been completed for ORR-MFE2. Dosimetry wires (Fe, Ni, Ti, and Co-Al) have been packaged in small aluminum capsules (~ 0.045 " diameter by 0.25" long) and sent to ORNL. These capsules will be placed in small holes - drilled in the thermocouple supports in each of the sixteen materials capsules on levels 1 through 4. A single tube will contain all four wires and HAFM materials will be loaded in a separate tube. Both tubes will run the entire length of the assembly to map fluence gradients. The irradiation is scheduled to start in late July and should last about one year.

3. Review of the Fusion Materials Irradiation Test Facility (FMIT)

A comprehensive review of the Advance Title I Report for the FMIT facility to be built at HEDL was held in Richland, Washington on May 10-11, 1978. The review panel consisted primarily of members of the Damage and Fundamental Studies (DAFS) Task Group of DOE, augmented by outside consultants. The main conclusions of the review were: (1) the dosimetry cell adjacent to the test cell was probably unnecessary, (2) a second test cell should be added to increase the plant factor, (3) test cells should be inerted with He for experimental and safety reasons, (4) cell handling procedures should be better developed and possibly redesigned, and (5) close cooperation was urged between the LASL accelerator design team and the HEDL staff to integrate beam diagnostic and dosimetry systems.

C. Alloy Development

1. Hydrogen Permeation and Materials Behavior in Alloys of Interest to the Fusion Power Program

Recent emphasis in this program has been directed towards (1) studies of hydrogen permeation in representative nickel- and titanium-base alloys and (2) construction of a small (~ 0.5 -liter capacity) stainless-steel-clad vanadium alloy lithium loop. Progress in these two areas during the third quarter of FY-1978 is summarized below.

a. Hydrogen Permeation Studies

E. H. Van Deventer and V. A. Maroni, Chemical Engineering Division

Hydrogen Permeation Studies on Inconel-718: A hydrogen permeation investigation of Inconel-718 was carried out using the apparatus and procedures described in previous publications.¹⁻³ Measurements were made in the temperature range from 100 to 800°C using hydrogen driving pressures that ranged from 10^{-1} to 10^4 Pa. The data obtained in this study for driving pressures greater than 3 Pa are plotted in Figure I-11 together with the permeability curve reported by Robertson⁴ for Inconel-718. Also included in Figure I-11 is Robertson's permeability curve for pure nickel.⁵ The measurements upon which Robertson's curve for Inconel-718 are based, were made using hydrogen driving pressures in the range 10^5 to 3×10^5 Pa. The combined results of our work and his are, therefore, in excellent agreement for hydrogen pressures which span the range 3 to 3×10^5 Pa. Although final refinements to our data remain to be made, a preliminary analysis of our data (as given in Figure I-11) has yielded the permeation equation

$$\phi_{\text{Inconel-718}} = 0.059 \exp(-13,373/RT) \quad (1)$$

where ϕ is in the units $\text{cm}^3(\text{STP})/\text{m}\cdot\text{s}\cdot\text{kPa}^{1/2}$, R is equal to 1.987, and T is the Kelvin temperature. Robertson reports a range of preexponential and activation terms for Inconel-718 which depend on the heat treatment used for selected Inconel-718 specimens in his work. His preexponential and activation terms (in the units used in equation 1) varied from 0.043 to 0.096 and 12,710 to 14,110, respectively. These ranges bracket the values given in equation 1.

¹E. H. Van Deventer, "Hydrogen Permeability of Haynes Alloy-188," J. Nucl. Mater. 66(3), 325-328 (1977).

²E. H. Van Deventer, et al., "Effects of Surface Impurity Layers on the Hydrogen Permeability of Vanadium," J. Nucl. Mater. 64(3), 241-248 (1977).

³V. A. Maroni, et al., Hydrogen Permeation of Type-316 Stainless Steel, Alloy Development for Irradiation Performance Progress Report for the Period Ending March 31, 1978, U.S. Dept. of Energy Report DOE/ET-0058/1 (78), p. 215.

⁴W. M. Robertson, "Hydrogen Permeation and Diffusion in Inconel 718 and Incoloy 903," Metall. Trans. 8A(11), 1709-1712 (1977).

⁵W. M. Robertson, "Hydrogen Permeation, Diffusion, and Solution in Nickel," Zeit. Metallkunde 64, 436-443 (1973).

Figure I-11. Hydrogen permeation data for Inconel-718.

At hydrogen driving pressures below 3 Pa, we observed the onset of departures from a square-root-of-pressure dependence in isothermal permeation experiments. In fact, log-log plots of permeation rate versus hydrogen pressure yielded slopes closer to 1.0 than to 0.5 for data collected in the 10^{-1} to 3 Pa range. This positive evidence of an oxide-film-limited region⁶ below ~ 3 Pa (under our experimental conditions) is most probably the result of surface oxide scales that are stabilized by the aluminum (~ 0.5 wt %) present in Inconel-718. Microscopic and microprobe analyses of the studied Inconel-718 specimen (to characterize the surface and bulk microstructure) are underway and will be reported on in a subsequent progress statement.

Hydrogen Interactions with Titanium-Base Alloys: In the face of current interest⁷ in titanium-base alloys as fusion reactor construction materials, we have initiated a series of studies with selected titanium alloy compositions to determine their hydrogen uptake, diffusion, and permeation characteristics. Early efforts are focusing on the 64 (6% Al, 4% V) and 5621 (5% Al, 6% Sn, 2% Zr, 1% Mo, 0.25% Si) alloys. The preparation of a reliable permeation assembly compatible with our permeation apparatus has proven not to be a straightforward matter. The method that has been successfully used in all prior studies (which include stainless steels, nickel-base alloys, vanadium, niobium, copper, and rhenium) involves electron beam welding of a thin disc of the sample material between two stainless steel couplings.² This method did not work with either the 64 or 5621 alloys in that a ductile weld could not be formed. Subsequent correspondence with J. A. Davis at McDonnell Douglas (MDAC) confirmed that a ductile weld of a titanium alloy to stainless steel would be difficult if not impossible to achieve. The idea of making the couplings out of a niobium or vanadium alloy (to test a SS/Nb/Ti or SS/V/Ti weld configuration) was considered, but it was reasoned that these strong hydride-forming alloys (Nb or V) would complicate the attainment of stable hydrogen pressures in the upstream and downstream compartments of the permeation apparatus. An attempt to seal a 64 alloy specimen between two conflat flanges was expectedly unsuccessful.

A method which appears to be working at the time of writing involves machining knife edges into both sides of the titanium alloy disc and then locking the disc between two 2-1/8-inch-diameter conflat flanges using a nickel gasket on each side of the disc. In this configuration, four knife edge seals are made simultaneously. Accordingly, knife edges were machined into a 60-mil sample of the 5621S alloy (supplied by J. A. Davis, MDAC) and an assembly was constructed. This assembly has held vacuum in the hydrogen permeation apparatus for ~ 900 hours at 470°C . Out-gassing of the sample is progressing but at a slower than normal rate, due in part to the hydrogen already in the titanium and the other coupling hardware and in part to the fact that 470°C is a lower temperature than is normally used to out-gas all-welded assemblies (i.e., $\sim 800^\circ\text{C}$).

⁶ R. A. Strehlow, et al., "The Permeation of Hydrogen Isotopes Through Structural Metals at Low Pressures and Through Metals with Oxide Film Barriers," Nucl. Technol. 22, 127-137 (1974).

⁷ J. A. Davis, et al., Assessment of Titanium for Use in the First Wall/Blanket Structure of Fusion Power Reactors, Electric Power Research Institute, EPRI ER-386, 1977. Also, J. A. Davis, Selection of Titanium Alloys for the Path C Scoping Studies, Alloy Development for Irradiation Performance Prog. Report for Period Ending March 31, 1978. U.S. DOE/ET-0058/1(78), p. 109.

b. Tests of Vanadium Alloy Performance in a Liquid Lithium Environment

D. L. Smith, Materials Science Division, and V. A. Maroni,
Chemical Engineering Division

Work on the stainless-steel-clad vanadium-alloy (V-15 Cr) lithium loop is continuing. The clad loop has been completely assembled and the clad vanadium-wire flowmeter leads have been attached. Figure I-12 is a photograph of the bare loop with the electromagnetic pump and the magnetic flowmeter installed on the two horizontal legs. A double valve arrangement has been incorporated into the design to permit insertion of test specimens and removal of lithium samples. A fill reservoir has been assembled and will be attached to the vertical leg just below the valves. A vacuum/gas access penetration to the annular region between the vanadium alloy and the cladding has been provided to monitor and control the atmosphere in the interface region.

Figure I-12. Photograph of the stainless-steel-clad vanadium alloy loop prior to attachment of heaters and insulation.

2. Microstructure of Irradiated Fusion Reactor First-wall Materials - Modeling

B. Okray Hall, Materials Science Division

A comprehensive time-dependent nucleation and growth model that includes dislocation loops, voids, and helium bubbles is being developed to describe the effects of simultaneous helium production and displacement damage on microstructural evolution in metals. The modeling complements the dual-ion irradiation studies and is designed to aid in interpretation of the experimental results.

a. Model Calculations

The basic interstitial- and vacancy-cluster nucleation model was modified to include interstitial trapping by an adaptation of the effective diffusion coefficient introduced by Schilling and Schroeder.¹ They considered impurities as immobile traps and derived an effective defect-diffusion coefficient D^+ , dependent on trap concentration C_t , trapping time τ , and capture radius R_t . Use of the simplest form of interaction potential, a square well, was sufficient for our purposes; the capture radius of a single jump distance is assumed, and the trapping time is then inversely proportional to the jump frequency ν_t from the trap site to the nearest-neighbor site:

$$\nu_t = \nu_o \exp[-(E_m^I + E_B)/kT],$$

where ν_o is the attempt frequency (assumed equal to that in the pure metal), E_m^I is the interstitial motion energy, T is temperature, and E_B is the interstitial-solute binding energy. The effective diffusion coefficient is

$$D^+ = D_i/[1 + 4\pi C_t R_t D_i (\tau - \tau_o)],$$

where D_i is the interstitial diffusion coefficient in the pure metal, and τ_o is the "trapping" time at a host-atom site. The factor $(\tau - \tau_o)$ replaces the factor τ used in reference 1, since the earlier formulation neglected to consider that only trapping time in excess of the host-atom trapping time affects the diffusion coefficient.

Materials parameters used in the calculations are given in Table I-5. The temperature was 460°C, the displacement rate was 2.75×10^{-4} dpa-s⁻¹, the sink-annihilation probability was 10^{-6} , and the bias was 1.05.

The calculated number density, in atom fraction, for interstitial clusters of size ≥ 2 is plotted in Figure I-13 as a function of dose for pure nickel ($E_B = 0$) and for Ni-4 at. % Si alloys with interstitial-solute binding energies of 0.25, 0.50, 0.75, and 1.00 eV. The plot shows that the calculated cluster number density increases strongly, and the clustering process slows, as binding energy increases.

¹W. Schilling and K. Schroeder, Consultant Symposium on the Physics of Irradiation-Produced Voids, R. S. Nelson, Ed., AERE-R7934, 1975, p. 212.

Table I-5. Model Parameters^a

Quantity	Value
Lattice constant, a	3.52×10^{-8} cm
Burgers vector, b	$a/\sqrt{3}$
Core radius, r_0	$b/2$
Poisson ratio, ν	0.34
Shear modulus, μ	7.85×10^{11} dyne/cm ²
Stacking fault energy, γ	400 erg/cm ²
Jump distance, d	2.49×10^{-8} cm
Vacancy formation energy, E_f^V	1.60 eV
Vacancy migration energy, E_m^V	1.28 eV
Vacancy frequency energy, ν_O^V	5×10^{13} s ⁻¹
Interstitial formation energy, E_f^I	4.08 eV
Interstitial migration energy, E_m^I	0.15 eV
Interstitial frequency factor, ν_O^I	10^{13} s ⁻¹

^a B. Okray Hall, to be published.

The vacancy-cluster number density is affected through the decreased arrival rate of interstitials, and increases slightly with increasing binding energy.

b. Experimental Results

Experimental investigations on loop nucleation were performed by D. I. Potter under BES funding. Ni-4 at. % Si specimens were irradiated at $\sim 465^\circ\text{C}$ with 3.0-MeV $^{58}\text{Ni}^+$ ions. The ion flux of $\sim 2.3 \times 10^{11}$ ions-cm⁻²s⁻¹ produced a peak damage rate of approximately 3×10^{-4} displacements per atom per second (dpa-s⁻¹). After irradiation, specimens were sectioned at ~ 5000 Å from the surface and examined by TEM. Loops were observed under dynamical diffraction conditions with a $\langle 200 \rangle$ two-beam diffraction geometry [optimal conditions for sizing small Frank-faulted loops].² Figure I-14 shows the loop microstructures as a function of irradiation time at 465°C . The lowest dose, 0.05 dpa, produced loops with an average diameter of 56 Å [Figure I-14(a)]. The measured loop density at this dose was 4.78×10^{14} loops-cm⁻³. Irradiation to a higher dose of 0.11 [Figure I-14(b)] increases the average loop diameter to 115 Å. The number density of 4.34×10^{14} loops cm⁻³ was

²K. H. Katerbau, Phys. Status Solidi (a)38, p. 463 (1976).

Figure I-13. Calculated interstitial-cluster number density versus dose for several interstitial-solute binding energies. $T = 460^{\circ}\text{C}$, $\dot{n} = 2.75 \times 10^{-4} \text{ dpa-s}^{-1}$, $p = 10^{-6}$. Curves are labeled by binding energy in eV.

essentially unchanged from its value of 0.05 dpa. Irradiation to higher doses of 0.35 dpa [Figure I-14(c)] and 0.67 dpa (not shown) produced average loop diameters of 302 Å and 341 Å, respectively, and measured number densities of $4.97 \times 10^{14} \text{ loops cm}^{-3}$. No voids were observed in any of the specimens.

The geometry of the dislocation loops was investigated in detail and is briefly summarized in Figures I-14(c) and I-14(d). These micrographs show the same foil area imaged under similar diffraction conditions but tilted into $\sim (001)$ orientation in Figure I-14(c) and $\sim (0\bar{1}1)$ orientation in Figure I-14(d). Such tilting experiments showed that all the loops were contained on $\{111\}$ planes. Further, the images were used to identify the particular $\{111\}$ planes of the loops being imaged; e.g., loops labeled A are on $(\bar{1}11)$ while those labeled B are on $(1\bar{1}1)$. The sets of loops on the other two $\{111\}$ planes are barely visible because of the particular diffraction conditions used to produce Figures I-14(c) and I-14(d). The Tunstall criterion was used to establish that the loops are all interstitial in nature.

Figure I-14. Dislocation loop microstructures observed in Ni-4 at. % Si irradiated at $\sim 465^\circ\text{C}$. (a) 0.053 dpa, $w = s$ $g \sim 0$; (b) 0.11 dpa, $w \sim 1.6$; (c) 0.35 dpa, $w \sim 1.6$, $\sim (001)$ orientation; (d) same foil area as (c) at $\sim 15^\circ$ from $(0\bar{1}1)$ orientation.

Loop-size distributions are plotted in Figure I-15 for the four low-dose Ni-4 at. % Si samples. The areas beneath the curves are approximately equal, reflecting the constant value of the loop number density with dose.

c. Comparison of Calculations and Experiment

In the Ni-4 at. % Si specimens examined, the interstitial-loop number density was constant at $\sim 4.8 \times 10^{14} \text{ cm}^{-3}$, i.e., 5.4×10^{-9} atom fraction, for doses between 0.05 and 0.6 dpa. From Figure I-13, the interstitial-solute binding energy should be $\sim 0.25 \text{ eV}$, in good agreement with the value of $0.26 \pm 0.06 \text{ eV}$ obtained by Hossain and Brown,³ who performed HVEM experiments on pure Ni and dilute Ni-Si alloys. The observed average loop sizes, however, are consistent with higher binding energies and the associated delay in the clustering process.

³ M. K. Hossain and L. M. Brown, Rad. Effects **31**, p. 203 (1977).

Figure I-15. Dislocation-loop size distributions for Ni-4 at. % Si Specimens. (●) sample A-3, 0.053 dpa, $T = 452^\circ\text{C}$; (x) sample B-3, 0.114 dpa, $T = 469^\circ\text{C}$; (o) sample C-1, 0.33 dpa, $T = 462^\circ\text{C}$; (□) sample D-1, 0.66 dpa, $T = 464^\circ\text{C}$.

d. Conclusions

In the present work, we have incorporated interstitial-solute trapping in a time-dependent nucleation and growth model and found that (1) the calculated interstitial-cluster number density depends strongly on the choice of binding energy; (2) the time to reach saturation in interstitial-cluster number density increases as binding energy increases; and (3) the vacancy-cluster number density increases moderately as binding energy increases. The calculations were compared with experimental loop-number densities and size distributions obtained by TEM for a Ni-4 at. % Si alloy irradiated with Ni^+ ions to doses ranging from 0.05 to 0.6 dpa. The number-density data were consistent with an interstitial-solute binding energy of ~ 0.25 eV, but the average loop sizes were consistent with higher binding energies.

D. Special Purpose Materials

W. Primak, Solid State Science Division

It has been established that the most serious source of radiation damage to a vitreous silica window would be the contraction of the surface caused by the absorption of soft X-rays from impurity emission in the plasma. It was concluded from calculations of ionization effects based largely on data for fractional MeV electrons, that the surface might become crazed in about 1/50 the desired lifetime of the window. Thus, it was apparent that protective discardable cover slips would be required. Experimental data to confirm these predictions was sought.

Surface crazing of vitreous silica samples caused by protons and by helium ions having ranges comparable to the depths of the effects expected from the soft X-rays in the TFTR was examined. For affected depths greater than 2 μM , crazing was encountered at stresses of 9,000 to 20,000 psi, values not much greater than the normal breaking strength, 6,500 psi. At affected depths below 2 μM there was evidence of plastic flow stress relaxation at stresses from 25,000 to 140,000 psi, the stress apparently depending mainly on the rate of application by the radiation-induced contraction, the long term yield stress having the smaller values.

To determine the yield for the X-ray induced contraction in vitreous silica the dose rate in vitreous silica from X-rays from a Machlett OEG-60 tube operated at 50 kV peak voltage and mean current 45 mA in 60 Hz full wave rectification mode was determined with the Fricke dosimeter (oxidation of ferrous to ferric ion in aerated weak aqueous sulfuric acid solution). The radiation-induced contraction appeared to be about half as great for small doses as had been calculated from previous electron bombardment experiments.

Vitreous silica specimens were supplied for PLT irradiation.

Arrangements are being made to irradiate vitreous silica in RTNS-II to determine the fast neutron yield for the radiation-induced contraction of vitreous silica.

Behavior to be anticipated for alumina windows will be considered next.

II. EXPERIMENTAL POWER REACTOR

A. First-Wall/Limiter Design

D. L. Smith, J. Krazinski and H. Stevens, Fusion Power Program

Work has continued on the design of multiple radiatively-cooled poloidal limiters for a tokamak Experimental Power Reactor (EPR). The first-wall/limiter system being analyzed was discussed in the last quarterly report. The CINDA-3G computer code has been used to determine the effect of limiter thickness, burn cycle, and down-time on the thermal response of the graphite limiters and stainless steel first-wall.

Figure II-1 shows the calculated thermal response of the plasma-side surface of graphite limiters which cover 10% of the first-wall area. The nominal neutron wall loading was 1 MW/m^2 and the 80-s power cycle described previously was used.¹ The maximum surface temperature was approximately 2000°C for the 1, 2, and 4-cm-thick limiters. However, the cyclic temperature variation decreased from $\Delta T = 750^\circ\text{C}$ to $\Delta T = 330^\circ\text{C}$ as the limiter thickness was increased from 1 cm to 4 cm.

¹ D. L. Smith and I. Charak, "Thermal Response of Tokamak Reactor First Walls During Cyclic Plasma Burns," Proc. Seventh IEEE Symposium on Engineering Problems of Fusion Research, Knoxville, Tennessee (October 25-28, 1977) pp. 1465.

Figure II-1. Calculated thermal responses as a function of limiter thickness for multiple graphite limiters that cover 10% of first wall area.

Temperature variations of the plasma-side surface of the stainless steel wall (not covered by limiters) were also computed. The results for limiters of different thicknesses are shown in Figure II-2. Here the surface heat flux to the steel wall includes neutral transport, as well as both electromagnetic radiation from the plasma and thermal radiation from the limiters. Increasing the limiter thickness from 1 cm to 4 cm has reduced the cyclic temperature variation of the steel wall by about 10°C - from $\Delta T = 57^{\circ}\text{C}$ to $\Delta T = 47^{\circ}\text{C}$.

The effect of modifying the power profile is shown in Figure II-3. Calculations were carried out for the "standard" 80-s power cycle used earlier¹ as well as a "modified" power profile. In both cases the average neutron wall loading was 1 MW/m^2 . The thermal response of the plasma-side metal surface follows the general shape of the power profile. Modifying the power profile has reduced the temperature fluctuation of the steel wall by 10°C . Also shown in Figure II-3 is the temperature variation for a bare steel wall without a limiter.

To determine the effect of an extended down-time on the thermal responses, the down-time for the "standard" 80-s cycle was doubled from 15-s to 30-s. The maximum temperatures of the 2 and 4-cm-thick limiters decreased slightly from $\sim 2000^{\circ}\text{C}$ (as shown in Figure II-1) to 1970°C and 1950°C , respectively. The cyclic ΔT for the 4-cm-thick limiter increased from 330 to 420°C when the down-time is increased from 15 to 30 s. For the plasma-side surface of the bare steel wall, doubling the down-time increased the cyclic ΔT by less than 5°C .

The computational grid was also modified to determine the temperature distribution in the limiters. This allowed a calculation of the temperature in the center of the limiter. For 1, 2, and 4-cm-thick limiters, the maximum temperature during the burn occurred on the plasma-side surface. During the down-time, when both surfaces of the limiter were losing energy by radiation, the temperature in the center of the limiter exceeded the surface temperature. However, for the 80-s power cycle this temperature difference was less than 80°C .

B. Iron Shielding to Decrease Toroidal Field Ripple

L. R. Turner and H. C. Stevens, Fusion Power Program

The ripple in the toroidal field of a tokamak reactor due to the finite number of toroidal-field (TF) coils causes particle and energy losses from the plasma. This ripple can be held to a tolerable level, by using a relatively large number of TF coils, or by using larger TF coils than would otherwise be necessary, with the outer legs of the coils far from the plasma region. Using more coils hampers access to the plasma and makes assembly, operation and maintenance of the reactor more difficult. Making the TF coils larger increases the size of the overall system; both approaches increase the cost of the reactor.

However, incorporating some sections of ferromagnetic iron in the neutron shield of the reactor is a simpler way to reduce the ripple and thus allow

Figure II-2. Calculated thermal responses of stainless steel first wall with 1, 2, and 4-cm-thick graphite limiters that cover 10% of first wall.

Figure II-3. Effect of power profile on the thermal response of stainless steel first wall with 1-cm-thick graphite limiters that cover 10% of wall area.

a tokamak with fewer TF coils. As shown below, the sixteen TF coils of the Argonne Experimental Power Reactor (EPR) design can be replaced by twelve coils, with a decrease in field ripple, if ferro-magnetic shield sections are used. Using twelve rather than sixteen coils allows many significant improvements. The process of ripple reduction and the resulting advantages are described in more detail in Reference 1.

1. Ripple Reduction with Iron

Near the outer legs of the TF coils the flux lines bulge out between coils, leading to a field that is higher in the plane of a TF coil than it is in a plane between coils. If iron is placed radially inward of the outer leg of a TF coil, the iron will produce a flux that increases the field in the gap between coils and diminishes it in the plane of the coil. This will occur even if the iron is saturated.

To study the effect of the iron, the TF coil system of the Argonne Experimental Power Reactor (EPR) design of 1977 was used, with the number of coils and the size of the iron segments varied. The computation used the magnet design program GFUN-3D. The field-ripple reduction possible with 16, 12 or 8 TF coils is shown in Table II-1. In each case the ripple is measured at the outer radius of the plasma, 6.7 m. The field ripple is defined to be $(B_{\max} - B_{\min})/2\bar{B}$. For 16 or 12 coils, the iron fits conveniently within the coils; but for eight coils, the ripple could not be reduced to less than about four percent without adding so much iron that access would be impaired.

Table II-1. Field Ripple Reduction with Iron

Number of Coils	Total Volume of Iron	Field Ripple Without Iron	Field Ripple With Iron
16	40 m ³	0.63%	0.11%
12	58 m ³	2.28%	0.53%
8	162 m ³	8.0%	3.9%

The 12 coil case combines a satisfactory field ripple of 0.53% with desirable access improvements over the 16 coil case. The forces between the coils and the iron were calculated. Each iron segment was found to attract the coil next to it with a force of 15.8×10^6 Nt (3.5×10^6 lb). This force reduces the tension in the coil by about ten percent.

¹ L. R. Turner, S-T. Wang, and H. C. Stevens, "Iron Shielding to Decrease Toroidal Field Ripple in a Tokamak Reactor," Third ANS Topical Meeting on the Technology of Controlled Nuclear Fusion, Santa Fe, New Mexico, May, 1978.

2. Design of the Iron Laminate Assemblies

General design features are shown in Figure II-4. The iron laminate assembly inserts are c-shaped measuring 5 meters in height, 1.2 meters in width and 0.8 meters thick. The inserts are fabricated from 1 cm thick mild steel plates coated with a film insulator. The plates are assembled into a laminate arrangement forming a monolithic self-supporting structure. This unit is self-supporting and weighs about 40,000 kg. (88 tons).

The iron laminate assemblies are readily integrated into the existing EPR shielding as shown in Figures II-5 and II-6. Each assembly is located directly under the outer portion of a TF coil, twelve in all. The outermost 73 cm of shielding which consists of the lead mortar and aluminum outer skin are replaced by the 80 cm. thick iron laminate. The iron laminate is relatively free from the need for forced cooling for radiation attenuation heating effects. Induced forces acting on the iron laminate assembly tend to pull it out of the shield position into the TF coil. The force of 3.5×10^6 lb is easily restrained with dowel pins in the bottom of the assembly anchored to the fixed lower blanket shield support structure base. The upper portion of the assembly is restrained with lug clamps wherein all the shield block are joined together poloidally transmitting forces as a ring in tension.

Handling these large 88-ton iron laminate assemblies has been given special attention. Removal or replacement of the assembly is done in two stages after first having removed the adjacent shielding blocks. First a set of heavy duty casters are bolted to the lower portion of the assembly and a load carrying guide rail stub installed; similarly, a upper guide stabilizing rail and roller system are installed. The lower rail is brought into load bearing condition with hydraulic jacks and the iron laminate assembly rolled out from under the TF coil. The second operation simply makes use of the overhead crane to lift the assembly clear of the reactor.

3. System Advantages

There are substantial advantages and cost savings associated with the reduction in the number of TF coils in the EPR reactor. Fewer, larger components yield a savings in fabrication which is proportional to the ratio of the number of coils. Cost savings are realized by reducing the number of cryostats and vacuum jackets by four. There is a 30% increase in the weight of each coil; but that results in little or no economic penalty. Fabrication of the inner cryostat and insulation jacket is simplified with 25% fewer joints along with greater assessibility for assembly. Fewer coil supports are required, with consequent lower resultant heat losses, yielding smaller refrigeration needs and savings in the cryogenics system.

Figure II-5 shows a tangential beam line arrangement which is restricted in a 16 TF coil system but fits into a 12 coil system. This feature would become very important if future development shows that neutral beam injection at these angles into the plasma is required.

Figure II-4. A Field Ripple Insert, Showing Iron Laminations and Containment.

Figure II-5. Experimental Power Reactor Plan View.

Figure II-6. Elevation section of experimental power reactor, showing location of field ripple insert.

Figure II-6 shows a 50% increase in space between TF coils, 2.9 m vs 2 m for a 12 vs 16 coil system. Design of structures to support the blanket and shield from below becomes easier and less costly. Overhead operations required for maintenance activities become easier and will result in faster operations and less complicated equipment.

Most of the components within the umbrella of the TF coils are sized on the basis of the number of TF coils; the fewer the number of TF coils, the fewer the number of components, although the components will be of slightly larger size. This factor leads to lowering the total cost by 20 to 25% of the total fabrication costs of the EPR shield, blanket, vacuum vessel, support structures, etc. In addition, disassembly and maintenance operations are easier and more quickly handled, reflecting an operational savings in down time.

III. FUSION SYSTEMS ENGINEERING

A. Development of Blanket Processing Technology for Fusion Reactors

The program under way in the Chemical Engineering Division to initiate the development of processing technology for liquid lithium fusion reactor blankets is continuing. Progress during the third quarter of FY-1978 is summarized below.

1. Lithium Processing Test Loop (LPTL)

J. R. Weston, J. B. Hines (Student Aide) and V. A. Maroni, Chemical Engineering Division

Early in the third quarter of FY-1978, the third in a series of heater failures on the LPTL reservoir tank necessitated a shut-down of the facility for refurbishing. During the shut-down the failed heaters were repaired and/or replaced, and other remaining fabrication work on the LPTL salt-tank control systems was completed in preparation for the salt-extraction tests scheduled to be performed during the fourth quarter of FY-1978. Following the shut-down (which occurred at about 2500 hours of operation) and the repairs, the LPTL was brought back on line and at the end of the third quarter had gone through its 3000th hour of operation.

The cause of the heater failures (investigated in conjunction with the manufacturer) appeared to be a consequence of excessive moisture in the cold-junction end of the heater. This generally results from insufficient bake-out of the ceramic potting material used in the cold junction and can usually be detected after manufacture by measuring the lead-to-sheath resistance. Accordingly, the manufacturer repaired the faulty heaters and a number of other unfailed heaters that showed signs of having a lower-than-desirable lead-to-sheath resistance. (We believe that this type of problem with cable heaters can for the most part be avoided by specifying a lead-to-sheath resistance of 50,000 megohm-feet or greater.)

Experiments currently in progress on the LPTL are aimed at completion of the cold trap/getter trap tests that were underway at the time of the recent shut-down. The cold trap temperature was reduced to a level just above incipient plugging (i.e., near the melting point of pure lithium) and the system was allowed to come to equilibrium. Thereafter, we have been raising the cold trap temperature in 40 to 50°C increments and reestablishing a new equilibrium. At each equilibrium cold trap temperature the LPTL reservoir tank is sampled for oxygen, nitrogen and carbon. Analytical work on the samples taken to date is in progress. Results from this entire series of tests will be presented in a subsequent progress report.

2. Support Activities

This section summarizes the results of a number of activities being carried out to support LPTL operations.

a. Lithium Mini-Test Loop (LMTL) Studies

R. M. Yonco and J. B. Hines (Student Aide), Chemical Engineering Division

Early in the third quarter of FY-1978, a small leak developed in the pump channel of the LMTL. The pump channel section (304-SS) was removed and replaced with a new section and the LMTL was brought back into operation within four weeks of the shut-down. The failure occurred along one of the edges of the rectangularly deformed channel, in a region where the residual stresses from the cold-working operation (to produce the rectangular cross-section) are expected to be quite high. Samples from the annealed (circular) and cold-worked (rectangular) regions of the channel are being subjected to analysis at Argonne, at Oak Ridge National Laboratory (by J. H. DeVan) and at the Liquid Metal Engineering Center (by N. J. Hoffman). Preliminary results from the ORNL and ANL analyses gave relatively little evidence of serious surface or intergranular attack in the samples from annealed regions. This is especially encouraging with respect to the usefulness of 300-series stainless steels as containment materials for liquid lithium, considering that the LMTL had operated for over 7200 hours at 450 to 500°C in lithium with ~ 600 wppm nitrogen prior to the pump channel replacement. The only evidence of severe attack has been that observed along the acutely curved and highly stressed edges of the rectangular section. This type of stress-corrosion problem should be avoidable in future systems by implementing proper forming and annealing procedures or by eliminating cold-worked sections of tubing or pipe.

A nitrogen sample taken from the LMTL several days after the startup following the pump channel replacement showed essentially the same level of nitrogen (~ 600 ± 100 wppm) as had been present before the failure. This is a positive indication that in-air repairs to lithium loop systems can be made without introduction of excessive amounts of ambient impurities. The procedure used to make the repair is one that has been successfully employed on sodium loop systems for many years. A report describing the procedure and its application to lithium loops is in preparation.

In addition to the pump channel failure, there is some evidence that one of the two Nupro Type-T bellows valves on the LMTL has failed internally. The valve stem seizes up during attempts to lower it, and the seating is inadequate to block flow. These valves have a stellite seat (a cobalt-nickel alloy) and it is quite probable that the seat material has been badly eroded by the lithium. (In the analysis of the failed pump channel section described above, ion microprobe scans detected measurable quantities of cobalt along the inner surfaces that were exposed to lithium.) Of the two valves on the LMTL the valve that experienced the internal failure is one that has been deliberately operated several hundred times since startup of the LMTL. The second valve still appears to be operating properly (in a mechanical sense), but there is no way to test its ability to shut down flow, as it is in parallel with the failed valve.

In addition to the nitrogen analyses routinely done on filtered samples from the LMTL and LPTL, we have recently established the capability for doing carbon analyses. This seems to be especially appropriate in view of the

recent finding¹ that characterized the interaction of nitrogen and carbon in lithium to form Li_2NCN . Samples taken from the LMTL after ~ 8000 hours of operation contained in the range from 17 to 47 wppm carbon and 600 ± 100 wppm nitrogen, these amounts being far from the stoichiometric requirements of Li_2NCN . A sample of lithium cyanamide is being prepared to further study its interactions in lithium, especially with respect to the ability of the analytical methods to detect nitrogen and carbon that is introduced to lithium as cyanamide.

b. LPTL Salt Tank Mock-up Studies

R. W. Peterman (Cooperative Student), Chemical Engineering Division

Experiments with the half-scale glass mockup of the LPTL salt extraction tank have continued. A color film of the operating sequence has been taken and is currently being worked into a movie by the ANL Film and Video Group. The mockup is also being used to study the effect on mixing kinetics of (1) stirrer shape, (2) stirrer location, and (3) stirrer speed. It is expected that the findings of these tests will be useful in defining suitable mixing conditions for initial experiments with the LPTL salt tank. For example, we have already found that optimum extraction kinetics occur when the stirrer is positioned so that it is approximately half in the upper phase and half in the lower phase. Also, in studies done with the mockup (where immiscible aqueous and organic phases serve as substitutes for the lithium and molten salt, respectively) the extraction rate appears to be proportional to stirrer speed to the ~ 0.8 power.

c. Analytical Method Development

E. Veleckis and J. Magee (Resident Student Associate), Chemical Engineering Division

A Toepler pumping apparatus,² previously employed to determine the solubility of LiD in liquid lithium,² has been adapted for assaying deuterium in samples of lithium taken from the Lithium Processing Test Loop (LPTL). The analytical procedure is based on the thermal decomposition of LiD in solution in lithium. Molten lithium is sampled from the LPTL by withdrawing ~ 1 g of filtered melt into a small stainless steel tube. The tube is cut into two sections and sealed (in vacuo) in a thin-walled iron capsule by electron beam welding. The capsule is placed in a fused silica tube, connected to the Toepler pumping system and heated at 850°C for ~ 10 days. (The walls of the iron capsule act like a semipermeable membrane that confines lithium vapor but permits the permeation of deuterium.) The initial series of one-gram loop samples are expected to have from 10 to 100 wppm of deuterium. When

¹ M. G. Down, et al., "Synthesis of the Dilithium Salt of Cyanamide in Liquid Lithium; X-Ray Crystal Structure of Li_2NCN ," *J. Chem. Soc./Chem. Comm.* 1978, 52 (1978).

² E. Veleckis, R. M. Yonco and V. A. Maroni, "Solubility of Lithium Deuteride in Liquid Lithium," *J. Less-Common Metals* 55, 85 (1977).

collected by the pumping system this concentration corresponds to 15 to 150 $\text{m}^3 \cdot \text{torr}$ of D_2 in the gas sampling bulb. Judging by our previous experience with similar samples,² this amount of deuterium should be sufficient for mass spectrometric analysis. The evolved gas is collected in a known volume and the deuterium content of the gas is determined with a mass spectrometer. Preliminary results from the first LPTL deuterium assay gave a value of 13 wppm, in good agreement with the estimated value of ~ 10 wppm, which was calculated from the controlled additions of LiD to the loop.

B. Safety Studies of Fusion Concepts

1. Analysis of Tritium Soaking Mechanisms

R. G. Clemmer and A. G. Rogers (Cooperative Student), Chemical Engineering Division

Since our previous progress report,¹ a number of modifications and improvements have been made on the air detritiation test apparatus. Addition of the new Overhoff tritium monitor and inclusion of more moisture traps provide increased flexibility for testing the performance of individual components on both the scrubber system and the monitoring system. The apparatus is shown schematically in Figure III-1. The right-hand side of the apparatus constitutes the tritium scrubber system, consisting of a catalytic oxidizer bed and a series of cold traps and molecular sieve beds. Each individual component of the scrubber system can be bypassed by switching a 4-way valve. The Overhoff meter can be used to monitor the tritium level in the scrubbed air returned to the test enclosure (OB position in Figure III-1), thereby permitting measurement of the efficiency of the scrubber system. A scrubber system efficiency of 100% can be simulated by pumping clean air or inert gas through the enclosure and exhausting the enclosure to the vent. The Triton meter is normally used to monitor the tritium level in the enclosure. In some tests, the Overhoff meter is used in series with the Triton (OA position in Figure III-1) to test monitoring accuracy.

A series of experiments was conducted to investigate tritium monitor performance. Since circulation in the Overhoff ionization chamber is produced by a different circulation pump in each position (OA and OB in Figure III-1), the pressure in the 2 & Kanne' chamber is not always the same; hence, it was necessary to calibrate the meter at each position. During the course of this calibration an unexpected result occurred (Figure III-2) when the apparatus was left over the weekend at a concentration of $40 \mu\text{Ci}/\text{m}^3$. In contrast to the well-behaved data for the two monitoring positions "A" and "B" ("A" corresponding to OA and "B" corresponding to OB in Figure III-1), the sixty-hour exposure to tritium had produced an apparent "memory" effect as indicated by the observation that curves A* and B* in Figure III-2 are significantly higher than the corresponding curves A and B. Further, the

¹ C. C. Baker, et al., "Fusion Power Program Quarterly Progress Report, January-March, 1978," Argonne National Laboratory, ANL/FPP-78-1 (1978).

T — TRITON MONITOR

OA }
OB } OVERHOFF MONITOR

E — TEST ENCLOSURE

G — PRESSURE GAUGE

I — AIR OR He INLET

EX — EXHAUST TO VENT

MS1 }
MS2 } MOLECULAR SIEVE BEDS

CT1 }
CT2 } COLD TRAPS

CAT — CATALYST BED

FM — FLOWMETER

Figure III-1. Air detrinitiation test apparatus

Figure III-2. Monitor memory tests with the Overhoff Meter. (A and B represent positions OA and OB in Figure III-1. All traps and beds were by-passed for B runs.)

curves no longer intercept the origin at zero. The result has significant implications with respect to monitoring of a large release of tritium in a fusion reactor facility. A hypothetical release of 10 g (10^5 Ci) in a reactor hall of 10^5 m³ volume would be expected to create significant difficulties in continuously monitoring tritium levels (as they return to the RCG level of 5 μ Ci/m³) with any degree of accuracy, using a single meter.

A number of additional experiments were carried out to test the two monitors for memory effects. A typical test comparing the two meters is shown in Figure III-3. Exposure to an initial level of as low as 100 μ Ci/m³ for a period of a few minutes is sufficient to cause an observable effect upon the Overhoff meter. It is clear that the Overhoff, with its 2 l Kanne' chamber, has more a substantial memory effect than the Triton which has a 10 l chamber. This increased memory effect displayed by the Overhoff meter may be due in part to the higher surface-to-volume ratio associated with its chamber.

Subsequent tests have attempted to measure memory effects in the Triton monitor by loading the enclosure to levels as high as 1000 μ Ci/m³ and following the levels with the Triton while keeping the Overhoff meter out of the circuit until the levels are reduced to 20-30 μ Ci/m³. No detectable memory effects were observed for the Triton in these tests.

Measurements of scrubber system performance indicated that the overall efficiencies (for tritium removal) were $\sim$ 95% in He and $\sim$ 96% in air. It was found through a series of experiments that the efficiency using one type-4A molecular sieve bed at 0°C could not be improved by adding either of the following: (1) a cold trap at 70°K (He atmosphere) or (2) a molecular sieve bed at 70°K (He atmosphere). It was concluded that the catalyst efficiency (for conversion of T₂ to T₂O) is about 96% and the moisture trapping efficiency is near 100%. The level of oxidized tritium (HTO, T₂O) in the atmosphere returned to the enclosure should be low (i.e., < 1% of the scrubber inlet level).

C. Energy Storage and Power Supply Requirements for Commercial Fusion Reactors

1. Inside Equilibrium-Field Coil Assessment

K. Evans, L. Turner, J. N. Brooks and H. C. Stevens, Fusion Power Program

An equilibrium-field (EF) coil design, for the 7-m reference reactor, was developed using an inside-the-TF (toroidal-field) coil location but still outside the radiation shield. The purpose of this design was to see whether the engineering difficulties of an inside location would be worth the savings in stored energy. The inside coils were designed to be approximately equally spaced 1-m from the plasma with the exclusions that: (a) no coils were lower than 15° from the minor axis on the in-board side; and (b) no coils were lower than 2-m on the out-board side. These constraints are necessary to prevent interference with beam ports and other components.

Figure III-3. Memory tests using both the Triton and Overhoff Monitors.

The design resulted in a stored energy of 0.7 GJ, as compared to 2.0 GJ for the outside design. Since the inside coils would most likely be normal conducting, there would be a substantial I^2R loss of the order of 10^1 s of MW. We have concluded that for this type of reactor at least, an inside EF location is probably inferior to an outside one. [An important qualification, however, is that for the moderate β (7%) and the moderate non-circularity ($K = 1.3$) of this reactor, an outside EF design is possible; for higher elongations and/or β one might have to go to an inside design to obtain the correct poloidal field pattern and plasma stability.]

2. Power Supply and Energy Storage Requirements

J. N. Brooks and R. L. Kustom, Fusion Power Program

An analysis of the power supply and energy storage requirements has been made for the reference 7-m tokamak reactor. A variety of power supply/energy storage systems were examined ranging from the conventional, presently available, to those needing development. In general, substantial reductions in the power supply requirements and costs seem possible over previous estimates,¹ because of a number of cost savings techniques made plausible by some encouraging information from present tokamak experiments. Figure III-4 shows the voltage and current waveforms for a burn cycle when a conventional power system is used. In this example both the OH and EF coils are driven by a thyristor type rectifier/inverter power supply both operating out of a motor-generator-flywheel (MGF) set. For the case shown, the OH current is ramped up in a time " Δt_{OH} " = 8 s. Neutral beam heating (100 MW) is applied from 8-15 s to bring the plasma to an ignited equilibrium. Some beam power (~ 15 MW) is also applied during the first and last second of the cycle to overcome some of the radiation losses due to oxygen impurities. Shutdown is accomplished by first reducing the DT fuel density and the plasma temperature (not shown) and then ramping down the OH current to bring down the plasma current and terminate the discharge. During the burn cycle the EF current must be adjusted in a way requiring active control of the EF voltage, as shown. For this example, the maximum power supply requirements are OH power = 1330 MVA, EF power = 351 MVA, and MGF stored energy = 12.8 GJ.

The effect of different startup densities, beam power levels, confinement scaling, impurity concentration, and several other parameters on the power supply requirements has been examined in detail. The most important single parameter has been found to be the OH startup ramp time. Figure III-5 shows the cost of the power supply/energy storage system, for the conventional system described above, as a function of the ramp time, and for three assumed values of the maximum OH design field. This cost covers the MGF set and the OH and EF supplies but does not include the neutral beam supply. Figure III-5 also shows the net electrical power from the reactor as a function of Δt_{OH} and P_{OH}^{MAX} . This net power takes into account the different burn times and duty factors obtainable as a function of the available OH flux swing. As shown,

¹ M. A. Abdou, et al., "ANL Parametric Systems Studies," Argonne National Laboratory, ANL/FPP/TM-100 (1977).

Figure III-4. Voltage and current waveforms for a typical burn cycle using MGF set and thyristor ohmic heating and equilibrium field supplies.

Figure III-5. Power supply/energy storage system cost and net electrical power from the plant, as a function of OH startup ramp time, Δt_{OH} , and OH design field, B_{OH}^{MAX} .

the cost is very sensitive to Δt_{OH} for short times. Previously, a range of $\Delta t_{OH} = 2-4$ s was considered optimum because shorter ramp times impose very high β and power levels while longer ramp times resulted in excessive resistive volt-second losses. Operation in this 2-s window tended to favor the use of a homopolar generator for the OH system because the cost of a homopolar generator is basically determined by the energy transferred and not the power level, exactly the opposite characteristic of a thyristor supply. Recent plasma physics experimental results and improved plasma engineering that point to classical resistivity (no anomaly), less oxygen contamination and the feasibility of low density startup have now substantially extended what appears to be a feasible range of Δt_{OH} . Longer ramp times still use more resistive volt-seconds but not by a prohibitive amount. For these longer times, the power supply system cost for a conventional system is estimated in the $\sim 60-100$ M range.

D. Blanket/Shield Design Evaluation for Commercial Fusion Reactors

D. L. Smith, C. Trachsel*, D. Bowers*, R. Clemmer, B. Cramer*, J. Davis*, G. Fuller*, J. Jung, J. Krazinski, H. Stevens and C. Youngdahl, Fusion Power Program

1. General Blanket Design Considerations

A study of tokamak reactor first wall/blanket/shield technology is being conducted in cooperation with McDonnell Douglas Astronautics Company (MDAC). The objectives of this program are the identification of key technological limitations for various tritium-breeding-blanket design concepts, establishment of a basis for assessment and comparison of the design features of each concept, and development of optimized blanket designs. The approach being used involves a review of previously proposed blanket designs, analysis of critical technological problems and design features associated with each of the blanket concepts, and a detailed evaluation of the most tractable design concepts. Initial phases of the program involve the evaluation of tritium-breeding blanket concepts according to the proposed coolant. The ANL effort has concentrated on evaluation of lithium- and water-cooled blanket designs while the MDAC effort has focused on helium- and molten salt-cooled designs. A joint effort has been undertaken to provide a consistent set of materials property data that will be used for analysis of all blanket concepts. The neutronics and tritium processing analyses are being conducted at ANL and the overall systems analyses are being coordinated by MDAC. A generic 2400 MWt tokamak power reactor with a major radius of 7.0 m, a minor radius of 2.3 m and a slightly elongated plasma is used for the blanket analyses.

The two main aspects of the materials considerations are a compilation of materials property data and the specification of the design criteria. The physical and mechanical properties data, including effects of neutron radiation, for the five representative structural alloys in Table III-1 are being assembled in a handbook. The stress criteria are based on elevated temperature design criteria derived from ASME Code Case 1592 for Components in Elevated Temperature Service. Both the time-dependent allowable stress, S_m , and the time-dependent stress limit, S_t , are incorporated into the stress analysis.

* McDonnell Douglas Astronautics Company, St. Louis, Missouri.

Table III-1. Reference Materials Considered in Blanket Design Study

<u>Structural Materials</u>	<u>Breeding Materials</u>	<u>Coolants</u>
Fe-Alloy: 316 SS	Liquid Lithium	Liquid Lithium
Ni-Alloy: IN 625	Solid Li_2O	Helium
Ti-Alloy: Ti-6242	Solid Li_7Pb_2	Water (Steam)
V-Alloy: V-15Cr-5Ti		Liquid Sodium
Nb-Alloy: FS-85		Molten Salt

listed in the table are also being compiled. An assessment of the suitability of Li-Pb alloys as breeding materials for fusion reactors was carried out. (Such materials could conceivably be used as either solid or liquid tritium breeders.) If one desires to use a solid, the logical choice would be Li_7Pb_2 , the highest melting compound (m.p. = 999 K) in the Li-Pb phase diagram. Since sintering could occur at $\sim 2/3$ of the melting point, this material may be unsuitable for application as a solid breeder at temperatures above 650 K. The difficulty with liquid Li-Pb alloys is that liquid lead is generally not compatible with candidate structural alloys and severe corrosion problems can be anticipated.

A nuclear analysis of the tritium-breeding performance has been made for the three breeding systems, viz., liquid lithium, Li_2O and Li_7Pb_2 . The analysis focused on three major categories: (1) trend of variation of breeding ratio with breeding zone thickness, (2) effects of a graphite reflector and beryllium neutron multiplier on the breeding ratio, and (3) impact of tritium breeding on the minimum blanket/shield thickness requirements for protection of the toroidal-field coils. Preliminary results for a reference blanket with no reflector or neutron multiplier indicate that the minimum blanket thicknesses required to attain a breeding ratio of unity are 0.33, 0.22 and 0.34 m for Li, Li_2O and Li_7Pb_2 , respectively. Breeding ratios in excess of 1.3 are attainable with all three breeding materials. These relatively high breeding ratios will be necessary for blanket designs that incorporate a divertor and do not breed in the inner blanket. The effects of fractional fuel burnup and doubling time on the required breeding ratio have been analyzed and were reported last quarter.

2. Lithium-Cooled Blanket Design

In addition to the materials and neutronic considerations discussed above, the analysis of the lithium-cooled blanket concept has focused on mechanical design, thermal-hydraulic and stress considerations. A review of the characteristics of previously-proposed lithium-cooled blanket designs serves as a basis for the present analysis. The basic blanket geometry proposed, the impetus for the design, and the important conclusions regarding a lithium-cooled blanket have been summarized for the various designs. The major motivation for using lithium as a coolant are its excellent heat transfer characteristics, low operating pressures and the fact that it can be used both for breeding tritium and as accoolant. Major concerns regarding the use of lithium relate to materials compatibility, tritium extraction, chemical reactivity with certain gasses and water, and magnetohydrodynamic (MHD) effects. The MHD effects include increased pumping power requirements, enhanced pressure stresses in the containment and reduction in heat transfer caused by suppression of turbulence. However, a major conclusion of previous blanket studies is that with proper design of the blanket system, liquid lithium is a viable coolant. Acceptable pumping power requirements can be achieved and satisfactory breeding ratios can be obtained, even if only the outer blanket is used for breeding.

The present study on the mechanical design of a lithium-cooled blanket focuses on three major aspects of the design, viz., blanket module configuration, vacuum wall position and manifolding. Fabrication, assembly, maintenance, structural support and special features are considered within the framework of these three factors. The above parameters are used to formulate a mechanical design that then must sustain and uphold critical heat transfer, hydraulic and stress requirements. The variables and parameters that affect blanket designs are considered for three basic blanket configurations: a small number (12-36) of complete wedges of a torus, a moderate number (96-192) of relatively large slab-type segments, and a very large number (2000-3000) of small cell-type modules. Major advantages and disadvantages inherent to each blanket configuration are summarized as follows:

- Complete wedge: simplest in principle, least number of coolant connections and relatively short replacement interval, requires maximum disassembly of reactor, huge pieces and huge hot cells - currently favored by many in the fusion community.
- Large slab: relatively few pieces and moderate number of coolant connections, requires overhead in-vessel grappling apparatus, minimum disturbance of reactor for operation, short replacement interval - considered optimum concept.
- Small modules: many pieces and many connections required, difficult coolant manifolding, minimum disturbance of the reactor components, blind automated in-vessel machines required, minimum disassembly of the reactor required but long replacement interval - least favored by the fusion community.

The impact of vacuum wall position on blanket design is also being evaluated. The three vacuum wall concepts considered are: the first wall, the outer edge of the blanket in the high-temperature region, and outside the blanket in the low-temperature region. It is generally concluded that the first-wall vacuum-vessel approach does not extrapolate to commercial reactor applications. Either of the other two vacuum-wall concepts give viable blanket designs. Coolant supply and return lines for the blanket heat-transport system pose a serious problem in the blanket systems design. The criteria for coolant manifolding other than coolant transport is basically two-fold: to minimize the number of connections to be made and to place the connection points in favorable locations. A major advantage of the lithium coolant relative to helium coolant is the much smaller manifold-size requirements.

Tokamak fusion power reactors are expected to operate in a cyclic mode with limited burn times (the order of 1000 s). As a result, the first wall and blanket structure will be subjected to cyclical variations in temperature. Degradation of the mechanical integrity of the structure as a result of the cyclic thermal stresses arising from these temperature variations has been identified as a critical problem that can limit the design lifetime of the blanket. The lifetime of the first-wall region of the blanket can be extended if the magnitude of the temperature fluctuations can be reduced. Two methods of reducing the temperature fluctuations are considered. Multiple limiters can be used to absorb a large fraction of the transport energy losses from the plasma during the burn. The thermal inertia of the limiters provides a continuous heat source to the wall during short off time. A second method involves tailoring the lithium flow or lithium temperature to the burn cycle to reduce the temperature fluctuations. The latter effects have been analyzed for a lithium-cooled blanket module. The blanket cell is shown schematically in Figure III-6.¹ The effects of two design changes on the thermal characteristics of the cell were examined. The curves in Figure III-7 show the variation of maximum structural temperature with the diameter of the inlet pipe. The results are given for a stainless steel wall. Depending on the wall loading, these curves give a limit on the largest inlet tube diameter allowable without exceeding the maximum recommended operating temperature, i.e., < 500 C for 316 stainless steel. The effects of heat flow across the inlet tube of the module were also analyzed. If the walls of the inlet tube are thermally conducting, the maximum structural temperature of the module would increase.

Stress analyses are being performed to quantify the trade-offs between surface heat flux, first-wall lifetime and operating temperature for candidate structural materials and typical module configurations. Rules and criteria derived from ASME Code Case 1592 for Components in Elevated Temperature Service are being used in developing a procedure for working backward

¹ B. Misra and V. A. Maroni, "Thermal Hydraulic Analyses of Two Fusion Reactor First Wall/Blanket Concepts," Argonne National Laboratory, ANL/FPP/TM-94 (August, 1977).

Figure III-6. Model of Lithium-Cooled Blanket Cell.

Figure III-7. Maximum first-wall temperature as a function of cell diameter ratio for three neutron wall loadings.

from design lifetime to restrictions on average temperature and thermal load. For example, Figure III-8 shows results for a trough-shaped module (similar to UWMAK-I) made of Type 316 stainless steel. The radius of the module is 0.25 m, the coolant pressure is 1 MPa and a 30-minute burn cycle is assumed. Surface heat flux is shown as a function of average wall temperature for various design lifetimes. Two lifetime criteria from the Code were applied in obtaining the results. (1) The temperature- and time-dependent primary allowable stress S_{mt} was used to determine the minimum wall thickness corresponding to a primary-stress lifetime L_p , and (2) code curves for creep-fatigue interaction were used to relate cyclic lifetime L_c to the cyclic thermal strains produced by the surface heat flux. The assumption was then made that the best design would be obtained by setting L_c equal to L_p . These results, for several values of $L_p = L_c$, are shown as the solid curves (W_1) in Figure III-8. The dashed curve (W_0) is the result for the more traditional design technique, which allots multiples of a temperature-dependent allowable stress S_m to various categories of stress. Mechanical property data for other candidate structural materials, viz., selected nickel, titanium, vanadium and niobium alloys, although less abundant, are being incorporated into the same stress analysis to assess the operating limitations of the alternate structural materials.

3. Helium-Cooled Blanket Design

Pressurized helium offers several advantages for use as a fusion reactor coolant. For example, it does not produce any significant radioactive products, is not subject to MHD effects and possesses favorable characteristics for tritium containment and processing. However, helium coolant is subject to relatively high pumping power requirements and requires gas pressures of 50-70 atmospheres. Previously proposed helium-coolant blanket designs have been reviewed. Each of these designs was developed to meet differing requirements and some emphasized a particular design aspect such as maintenance, fabrication, etc., and may not have been completely analyzed for other factors such as life. Most designs assume neutron wall loads from 1 to 3 MW/m² and have stated a first-wall life of ~ 2 years. All designs operated at coolant pressures of ~ 50 atmospheres. Flow rates varied from 3 to 12 m/s while inlet and outlet temperatures varied from 77 to 450°C and 370 to 780°C, respectively. These studies have shown that lower structural temperatures provide longer life, particularly for stainless steel and nickel alloys, because materials properties are enhanced and radiation damage effects are reduced.

A major problem with the use of helium coolant is that large manifolding is required which results in loss of access space and increased blanket thickness (particularly the inner blanket). Manifold requirements for a reference 2300 MWt reactor are shown in Figure III-9. As indicated, if one inlet and outlet manifold is used in each of the 12 segments in the reactor, a 1.4-m diameter outlet and a 1.1-m diameter inlet is required. Since the cross-sectional area of the outlet duct is approximately twice that of the inlet duct, two outlet ducts the size of the inlet duct could be used. If each of 12 segments is further divided into four segments, the manifolding will require approximately 38-cm thicker inner blanket for manifold routing. Further

Figure III-8. Maximum first-wall surface heat flux as a function of wall temperature for three design lifetimes.

Figure III-9. Helium-coolant manifold sizes as a function of number of manifolds.

division of the 1/12 segment into 8 pieces could permit use of a design that requires approximately 16-cm additional thickness for the inner blanket for helium manifolding. This appears to be a near optimum manifolding system.

Maintenance of a helium-coolant system should be much simpler than a liquid coolant system primarily because gravity drawing or preheating to melt the coolant is not required. However, the larger helium coolant lines will impair access and make maintenance more difficult. Also, control of helium leakage would be more difficult than for other coolants.

A thermal analysis of a helium-cooled first wall was conducted in the present study. Temperatures were calculated for a first wall consisting of a series of tube banks. Calculations include the temperature difference across the coolant film as a function of coolant flow velocity and temperature. Figure III-10 is a plot of the film ΔT as a function of tube diameter for various coolant velocities. Relatively large flow velocities are required for reasonable ΔT values. The coolant temperature rise along the first-wall tube length is shown in Figure III-11 as a function of tube diameter and coolant velocity. From these types of results the maximum allowable wall loading has been calculated for various wall materials and operating conditions. Figure III-12 illustrates the results for stainless steel for $T_{\max} = 400^{\circ}\text{C}$, coolant flow velocity of 30 and 60 m/s with and without a divertor. The allowable wall loadings are $< 1 \text{ MW/m}^2$ for no divertor and $\sim 2 \text{ MW/m}^2$ with a divertor, which reduces the surface heat flux.

A structural analysis has also been conducted to predict the lifetime of the helium-cooled first wall. Tube diameter, tube-wall thickness, helium flow velocity and neutron wall load were varied over ranges of interest to determine optimum design conditions. The temperature gradients established in the tubes cause thermal stresses in both the circumferential and longitudinal directions. These stresses have been calculated based on two assumptions: (1) the tube is free to expand longitudinally but constrained against bending, and (2) the tube is completely unconstrained. Life of the helium tubes was calculated using fracture mechanics analysis where the tube life was dictated by the growth of existing flaws to a coolant leakage condition under the influence of cyclic stresses. The effect of tube-bending constraint on cyclic lifetime was shown to be very important. The lower cyclic stress levels associated with an unconstrained coolant tube result in considerably longer life than for the case where bending deflections are constrained. This result is demonstrated in Figure III-13 where the life of both constrained and unconstrained tubes is shown for the baseline variables shown in the figure. These lives are based on cyclic stresses in the longitudinal tube direction, which are higher than those in the hoop direction even for the case of the unconstrained tube. The neutron wall loading and helium flow velocity were shown to have a major impact on coolant tube life. The effect of coolant velocity on tube life is shown in Figure III-14 for a stainless steel wall under a 2.4 MW/m^2 neutron wall loading.

An evaluation of a helium-cooled canister design was conducted to determine its potential payoff. The primary advantage expected was to decrease the maximum material temperature by eliminating the coolant temperature rise across the front face that occurred in the tube design. In the canister

Figure III-10. Temperature drop across coolant film as a function of tube diameter.

Figure III-11. Helium coolant temperature rise per unit length of first-wall coolant tube.

Figure III-12. Maximum tube-wall temperature as a function of neutron wall loading for coolant velocities of 30 and 60 m/s.

Figure III-13. Effect of bending constraint on predicted life of helium-cooled first-wall tubes.

Figure III-14. Effect of helium flow velocity on predicted life of coolant tubes.

design the coolant flows directly to the front wall and flows across the first wall at high velocity. It is further heated in the blanket breeding/moderating materials prior to exiting. This design is similar to the Japanese tokamak canister and the tandem mirror module and can be configured as either a circular or elongated canister. From this work, two design concepts are recommended for further evaluation in subsequent phases of the program. These two designs are a small rectangular canister and a module with free floating tubes.

E. Neutronics Calculations for Fusion-Fission Hybrid Systems

Y. Gohar, Applied Physics Division

The neutronics and photonics of several fusion-fission hybrid blankets have been studied for an ANL assessment of the fusion-fission reactor concepts. The main concern of this study is to determine the nuclear performance for various blanket types. A uranium-lithium, thorium-lithium, and thorium-uranium-lithium blanket types are considered to determine the most important integral and differential quantities and to get a general picture for the neutron spectra for such type of blankets. Table III-2 gives the following integral quantities for eleven hybrid blankets.

- (1) (n,3n) reaction rates
- (2) (n,2n) reaction rates

Table III-2. Neutronic Characteristics of Different Fusion-Fission Hybrid Blankets

Blanket No.	1	3	6	4	2	5	7	8	9	10	11
Fuel region thickness (cm)	45	25	20	15	45	35	25	25	15	20	15
Fuel type thickness (cm)	45 NU*	25 NU	20 NU	15 NU	45 NU	35 NU	5 Th + 20 MU	10 Th + 15 NU	5 Th + 10 NU	10 Th + 10 NU	15 Th
Lithium region thickness (cm)	10	50	60	60	30	50	60	60	60	60	60
Neutron heating (MeV/fusion neutron)	153	143	136	124	148	131	98	71	78	63	28
Gamma heating (MeV/fusion neutron)	27	26	24	23	26	24	--	--	--	--	--
Total heating (MeV/fusion neutron)	180	169	160	147	174	155	--	--	--	--	--
Energy multiplication factor	13	12	11	10	12	11	--	--	--	--	--
Tritium breeding ratio	0.2	0.8	1.1	1.4	0.3	1.0	0.8	0.8	1.3	1.1	1.2
(n,3n) reaction rate per fusion neutron in the fuel region	0.14	0.13	0.13	0.12	0.14	0.13	0.15	0.15	0.13	0.15	0.13
(n,2n) reaction rate per fusion neutron in the fuel region	0.26	0.25	0.25	0.23	0.26	0.24	0.33	0.36	0.30	0.35	0.34
$^{238}\text{U}(n,\gamma)$ reaction rate per fusion neutron	2.94	2.25	1.89	1.45	2.79	1.99	1.44	0.92	0.76	0.64	0.0
$^{232}\text{Th}(n,\gamma)$ reaction rate per fusion neutron	0.0	0.0	0.0	0.0	0.0	0.0	0.41	0.67	0.35	0.62	0.68
(n,fission) reaction rate per fusion neutron	0.86	0.78	0.73	0.65	0.82	0.70	0.53	0.37	0.39	0.32	0.11

* Natural uranium.

- (3) (n,γ) reaction rates for ^{238}U and ^{232}Th
- (4) $(n,\alpha)\text{T}$ reaction rate for ^6Li
- (5) $(n,n')\text{T}$ reaction rate for ^7Li
- (6) Total neutron heating rate
- (7) Total gamma heating rate

In these blankets no attempt was made to optimize any quantity or to use enriched material which explains the unsatisfactory performance of some blankets.

This preliminary survey shows the following conclusions.

- a) The $(n,3n)$ and $(n,2n)$ reactions contribute one neutron for each DT fusion neutron.
- b) A tritium breeding ratio greater than 1 is possible.
- c) A production rate of 0.7 to 2.0 fissile atoms per DT fusion neutron can be achieved for blanket with tritium breeding ratio greater than 1.0.
- d) An energy multiplication factor greater than 10 is possible.

F. Cross Section Measurements, Evaluations and Techniques

1. Measurement of Cross Sections for the $^{27}\text{Al}(n,n'\gamma)^{27}\text{Al}$ Reaction Near Threshold

Donald L. Smith, Applied Physics Division

Cross-section ratios for production of 0.843-, 1.013- and 2.209-MeV gamma rays at 55 deg by the $^{27}\text{Al}(n,n'\gamma)^{27}\text{Al}$ reaction relative to fast-neutron fission of ^{235}U have been measured at intervals of ~ 0.05 MeV from threshold up to ~ 2.5 MeV with an average neutron energy resolution of ~ 0.08 MeV. These ratios and ENDF/B-IV fission cross sections were used to compute gamma-ray production cross sections. Gamma-ray angular distributions were measured at $E_n = 1.112, 1.310, 1.512, 1.714, 1.914, 2.118, 2.310, \text{ and } 2.512$ MeV. These distributions were found to be isotropic within the experimental errors. The experimental results are compared with data from the literature and with results from optical/statistical model calculations of neutron inelastic scattering from aluminum.

2. The Interaction of Fast Neutrons with ^{60}Ni

A. Smith, P. Guenther, P. Moldauer, D. Smith and J. Whalen, Applied Physics Division

Neutron total cross sections of ^{60}Ni are measured with broad resolutions from ≈ 0.5 to 5.0 MeV at intervals of $\lesssim 50$ keV. Differential elastic-neutron-scattering cross sections are measured from 1.5 to 4.0 MeV at intervals of $\lesssim 50$ keV over the scattered-neutron angular range of ≈ 20 -160 deg. Differential cross sections for the inelastic-neutron excitation of states at $1.342 \pm 0.013, 2.168 \pm 0.010, 2.304 \pm 0.026, 2.509 \pm 0.072, 2.636 \pm 0.019$ and 3.164 ± 0.041 MeV are measured. The experimental results are interpreted in

terms of optical-statistical and coupled-channels models with particular attention to the effects of fluctuations and the contributions of direct excitations of the collective-vibrational structures. The present results are correlated with sparse previously-reported experimental values and model predictions to obtain evaluated numerical neutron total and scattering cross-section files in the few MeV region.

IV. MAGNETIC SYSTEMS

A. Development of Cryostable Superconducting Pulsed Coils

S. H. Kim and S-T. Wang, Accelerator Research Facilities Division

Studies on basic cables, small test coils and the design of a 12 kA cable and a 1.5 MJ coil have been reported.^{1,2,3} During this quarter, the 12 kA cabling and 1.5 MJ coil fabrication have been completed. Also, a pair of 15 kA current leads was tested.

1. Fabrication of the 12 kA Cable

The 12 kA cable for the 1.5 MJ pulsed coil is fabricated by twisting 24 triplex conductors around an insulated stainless steel strip with a twist pitch of 22.5 cm. The first 25-m long cable was produced as a test of the cabling technique. This cable is compressed with heavy rollers from four sides of the cable as shown in Figures IV-1 and IV-2. The final cable finished dimensions are 3.78-cm wide and 0.74-cm thick. The cable cross section is shown in Figure IV-3. The compression during the cabling is to minimize any mechanical perturbations of the basic conductors during the pulsing of the coil. After cabling, the insulation between the stainless steel strip and the 24 triplex conductors was not damaged. However, about 5% degradation of the triplex recovery current was observed. It was found that this is partly due to the deformation of the soft soldering and partly due to the rather thick insulation film around seven-strand conductors. The total cable length was about 590-m long.

2. 15 kA Vapor-Cooled Current Leads Test

High-current pulsed coils need large vapor-cooled current leads to feed current from room temperature to liquid helium temperatures. Minimal heat-leak leads could be designed by utilizing the cold counter-flow helium boil-off gas. A pair of current leads with a capacity up to a dc current of 15 kA was purchased from American Magnets, Inc. These leads are 80-cm long with 7.6-cm diameter. They are tested without the 15-MJ coil by short circuiting the bottom tips of the leads with superconductors, as shown in Figure IV-4. When the liquid-helium level is about 25-cm below the bottom tip of the leads,

¹ C. C. Baker, et al., "Fusion Power Program Quarterly Progress Report," July-September, 1977," Argonne National Laboratory, ANL/FPP-77-4 (1977).

² S-T. Wang, S. H. Kim, W. F. Praeg and C. I. Krieger, "ANL Experimental Program for Pulsed Superconducting Coils," Proc. of 7th Symposium on Engineering Problems of Fusion Research, Knoxville, TN, October 25-28, 1977, IEEE Publ. No. 77CH1267-4-NPS, Vol. II, pp. 1322-1325.

³ S-T. Wang, et al., "Design and Development of Cryostable Superconducting Ohmic Heating Coils for a Tokamak," Presented at the Cryogenic Engineering Conference, August 2-5, 1977, University of Colorado, Boulder, Colorado.

Figure IV-1. Cabling process (before the cabling).

Figure IV-2. Cabling process (after the cabling).

Figure IV-3. A closeup of the 12 kA cable cross section.

the temperatures of the bottom tip and top tip of the leads were about 10-15 K and 95-100 K, respectively. With a dc current of 12 kA, the temperature of the upper parts of the leads increased about 10 K in 30 minutes. Figure IV-5 shows the top flange of the cryostat with frozen leads. Liquid helium boil-off is about 22 W without current and 30 W with a 12 kA current. This heat leak indicates that the leads are in optimal condition. However, stability of the leads with a current extending beyond 30 minutes is not known at this time.

3. 1.5 MJ Coil Fabrication

Coil winding with a spongy cable is a rather interesting experience of the pulsed coil program. The coil is composed of 18 helical layers with an average of 14.3 turns in each layer. Turn-to-turn insulations are provided by two layers of 0.02-cm thick glass-cloth tape and two layers of 0.01-cm thick Mylar tape. The winding layers for the first to tenth layers are separated by 0.48-cm thick G-10 strips and for the eleventh to eighteenth layers by 0.32-cm thick G-10 strips. Figure IV-6 is a closeup of the coil winding in the thirteenth layer. The G-10 bobbin has cooling channels in radial directions. G-10 strips make 0.64-cm wide cooling channels in the vertical direction. During the winding, the tension in the cable was increased gradually from 225 kg to 450 kg to provide a constant radial pressure in the coil. Total length of the coil used for the coil is about 510 cm. Nine potential taps have been installed in the coil to study possible conductor motion and six thermocouples to monitor temperature variation during the test of the coil. When the coil is charged, the average hoop stress in the cable is about 310 MN/m^2 . Part of the stress can be sustained by the stainless steel strip in the cable itself. To support the excess stress, 16 bands are placed outside the coil. Each band is made of 30 layers of 0.025-cm thick by 3.2-cm wide fiberglass.

4. 50 kA Cable Studies

A 50 kA cable design, illustrated in Figures IV-7 and IV-8, is based on the experience of fabricating the 12 kA cable. The triplex conductor in Figure IV-7 is the basic cable of the 12 kA cable. Four triplex conductors are twisted around on insulated multistrand stainless steel cable and this forms one of the 24 basic cables for the 50 kA cable. Since the stainless steel cable reduces the electrical contact within the triplex conductor to a certain degree, the seven-wire conductor is not insulated. This yields stronger ac coupling than desired. However, the cable has better current sharing and, thus, would be more cryostable. With 50% surface exposure to liquid helium, cryostable recovery heat flux is about 0.53 W/cm^2 . New England Wire, Inc., has been commissioned to produce a 23-m long cable by September, 1978.

The 50 kA cable will be wound in pancake form and tested in the 1.5 MJ pulsed external coil.

Figure IV-5. Top flange of the cryostat with frozen current leads.

Figure IV-6. A closeup of the thirteenth layer of the 1.5 MJ coil winding.

BASIC CABLE, DIA. 0.313"

Figure IV-7. Basic cable.

Figure IV-8. 50 kA cryostable cable.

B. Energy Storage and Transfer Program

R. Fuja, R. L. Kustom, R. Wehrle and R. P. Smith, Accelerator Research Facilities Division

1. Inductor Converter Bridge

The superconducting energy storage and transfer test facility (SESAT) has been reenergized with the following system modifications. An Intel 8080 microprocessor is employed as the controlling mechanism for the converter SCR trigger pulses. The microprocessor allows, through software, the generation of two sets of six SCR trigger pulses displaced from one another by up to 90 electrical degrees. With microprocessor control, the two sets of trigger pulses can be made to vary with respect to each other, at any time during a cycle, causing the energy transfer rate to go from maximum to minimum and back again.

Current transducers were installed in each leg of the superconducting coil leads. The transducers produce a 1-V signal for every 25 amperes of current in the coil. The output voltage of the transducers is well above the noise level generated by the switching SCR's. The current in each coil can be monitored throughout the cycle and the efficiency of energy transfer calculated.

The three goals of the SESAT test were to test the transducers and the microprocessor control system, to study the efficiency of energy transfer at various load and storage coil voltages, and to check the systems response to a step function change in driving voltage. Some of the test results are shown in Figure IV-9. The currents in the storage coil (top trace) and load coil (bottom trace) are shown as the transfer process proceeds from start to finish. Operating with a sequence of pulses, which produce a 90 degree leading phase shift, the current is transferred from the storage coil to the load coil in 2.5 seconds. The efficiency of energy transfer is 81%, which is 90% of the current at the start of the cycle ends up in the other coil at the end of the cycle. Figure IV-10 shows the storage coil discharging voltage and the load coil charging voltage at various times during the cycle. The pictures were taken with the bridge operating at the maximum transfer rate.

When the phase sequence between the two sets of six pulses was adjusted to obtain a slower energy transfer rate, the system would not transfer energy completely. When approximately 75% of the energy was transferred, the protection circuit across the storage coil would become activated and the transfer process would stop. The problem was analyzed and the cause determined to be a large peak voltage across the coil. This condition is normal during operation and modifications were made to accommodate the voltage levels. The tests will resume shortly and the remaining data obtained.

2. Homopolar Generator

The homopolar generator system (HOPE 1) continues to operate smoothly but the system has not transferred more than 6800 joules due to a computation problem in the power supply disconnect circuit. This problem will be investigated more fully during the next run.

50A/div 0.5s/div

Figure IV-9. Current transfer as a function of time.

100V/div 0.5ms/div
DELAY 100 μ s

100V/div 0.5ms/div
DELAY 1.0s

100V/div 0.5ms/div
DELAY 2.35s

Figure IV-10. Coil voltages at different points in time.

The data obtained from previous runs have been analyzed and the phenomena occurring between the voltage across and the current through the homopolar generator has been explained. The difference in the time when the current goes to zero and the voltage goes to a maximum is due to a phase shift between the voltage and current. This phase shift is caused by the resistance due to the silicon-controlled rectifiers (SCRs), the cabling and most of all to the brush drag on the generator. The coefficient of friction between the cylinder and brushes was measured and found to be 0.13. Taking all the losses into account, accounts for the 50% efficiency in energy transfer. A mechanical analysis of the dynamic system has been performed and the results are the same as if the electrical analog of the system was used in accounting for the performance of the generator.

C. Negative Ion Source Development

1. Direct Extraction H⁻ Source

J. A. Fasolo, Accelerator Research Facilities Division

After initial difficulties with control logic malfunctions due to SCR- and choke-generated noise, a number of changes in and rearrangements of components (SCRs, IC chips, diodes, capacitors, etc.) have been coupled with improved grounding, shielding and filtering to achieve the SCR-switched arc modulator design specifications. During start-up, the Penning discharge operates in a low-current, high-voltage mode. The high voltage is supplied by a small turn-on capacitor bank that can provide an initial voltage up to 800 V; a switching arrangement allows selection of either 10 μ F or 50 μ F of capacitance for the turn-on bank. When the source has been heated to operating temperature by the discharge, the discharge operates in a high-current, low-voltage mode if the supply of cesium vapor from an external boiler to the cathode is adequate. In this mode, the turn-on circuit is used to minimize the arc current rise time and the arc is sustained at a voltage ≤ 100 V by a large (50,000 μ F) capacitor bank that is switched in when the turn-on capacitor bank voltage drops to the desired sustaining level. The arc voltage pulse is terminated by reversing the voltage across an SCR in series with the source. The turn-off voltage is developed by discharging a 262 μ F capacitor bank into a 25 μ h choke.

Typically, arc currents and voltages have ranged from 90 A and 78 V for a 400 μ s pulse at 17 Hz to 150 A and 70 V for a 300 μ s pulse at 17.5 Hz. The average gas flow rates for these two cases were 0.062 and 0.081 Torr-liter/s, respectively.

The gas is pulsed with a modified Veeco PV-10 piezoelectric valve. The width of the drive voltage pulse of ≤ 100 volts is typically in the range of 53 to 150 μ s. The optimum delay between the start of the valve drive pulse and the start of the arc voltage pulse is about 300 μ s.

For the present discharge electrode configuration, which is similar to that of the Los Alamos Penning source,¹ the cesium vapor pressure appears to

¹ P. W. Allison, IEEE Trans on Nucl. Sci., Vol. NS-24, No. 3 (June, 1977).

be near optimum when the average discharge power is approximately 50 watts, as has been noted by Allison in reference 1. At lower values of average power, the arc may extinguish before the end of the arc voltage pulse and the arc current waveform is typically triangular or has an initial triangular section followed by a low level plateau. If the average power is significantly higher than 50 watts in this geometry (see Figures IV-11 and IV-12), the cesium vapor pressure becomes too high. When this happens, a coating of cesium on the insulator can short-circuit the source or cause the arc voltage to drop to a very low level (of the order of a volt). On some occasions, the partial shorts can be cleared by turning off the gas valve and letting the source cool while pulsing voltage into the partial short.

Excessively high cesium vapor pressures have also resulted in discharges damaging to stainless steel components in regions remote from the normal discharge region. On June 14, 1978, a short occurred in the circled region of Figure IV-11. A stainless steel alignment pin pressed into the molybdenum cathode was partially vaporized. The insulator was coated with vapor from the pin and molybdenum sputtered from the cathode. The stainless steel slit plate holder was badly pitted in the encircled regions of Figure IV-12. The faces of the arc-defining slots of the molybdenum anode were also badly pitted. Damage to the anode evidently occurs during arc start-up while the source is still cold and the arc is running in the low-current, high-voltage mode. Occasionally, a burst of pulses occurs in which the current increases to hundreds of amperes, if not limited by ballast resistors, and glowing particles are ejected from the source. When the high current pulses occur, the normal arc flashes are either very faint or not visible at all. These high-current pulses sometimes continue to occur even when the magnet and the gas flow are turned off; they are evidently fed by metallic vapors.

On June 28, 1978, another short similar to the one just described occurred. Damage to the stainless steel pin was even worse this time. On the advice of P. W. Allison, who had encountered similar difficulties with a mild steel pin in the Los Alamos source, the stainless steel pin was replaced with a molybdenum pin. On June 30, 1978, arcing was observed between the stainless steel clamp washer and the stainless steel damp ring of Figure IV-11 whenever the voltage was raised to several hundred volts. When the source was dismantled, it was found that the washer, which was not self-centering, was off-center. The arcing occurred in the region where the washer was closest to the clamp ring. The insulator surface in this region was coated with metal.

The stainless steel clamp washer has been replaced by a self-centering molybdenum clamp and the bore of the clamp ring has been enlarged and fitted with a molybdenum liner. At the present time the original stainless steel slit plate holder is still in use; it will be modified to accommodate molybdenum liners.

Progress has been made in debugging the regulated high voltage supply but this work has not received as much emphasis as has been given to debugging of the arc modulator and elimination of causes of source failures.

Figure IV-11. A cross section through the modified Penning source as of June 14, 1978. Parts 4, 5 and 6 are gas valve components, 7 is a clamp washer, 8 a clamp ring, 9 an insulator, 10 the anode holder, 11 the cathode, 12 the anode and 13 the extractor. Damage has occurred in the circled area.

0 1 2 3 cm

Figure IV-12. Another view of the Penning source. Damage to the slit plate holder has occurred in the encircled areas.

V. APPLIED PLASMA PHYSICS

A. Plasma Transport in Tokamaks

C. D. Boley, Applied Physics Division

The calculation of radial transport coefficients for a multispecies plasma in the collisional (Pfirsch-Schlüter) regime involves, in general, the inversion of N coupled integral equations in velocity space where N is the total number of ion species. By standard approximation schemes, the integral equations can be reduced to linear algebraic equations, so that one is faced with the inversion of $N \times N$ matrices. For realistic plasmas with several impurity constituents, this is a cumbersome problem, since the inversion must be carried out at every mesh point. Hirshman has proposed an approximation¹ that circumvents this problem and which, therefore, could be very useful. It was shown to give the correct limiting results for a plasma with constituents of disparate mass.¹ More generally, however, its validity is not so easily settled.

This approximation has now been tested in a more general system for which the complete transport coefficients have recently been obtained by analytical methods. The system consists of several disparate-mass species, the heaviest of which has an arbitrary number of charge states (the lighter species are electrons and singly ionized ions). The approximate results for this system were worked out by Hirshman¹ (who considered only two disparate-mass species, but the generalization is immediate), while the analytical results are a special case of the disparate-clump theory of Boley, Gelbard and Hirshman.² The latter results actually turn out to be much simpler than the former. For purposes of comparison, the most meaningful quantities to examine are the classical friction coefficients ℓ_{ab}^{11} , ℓ_{ab}^{12} and ℓ_{ab}^{22} , in which the indices a and b label individual charge states of the heavy complex ion. The classical friction coefficients connecting other combinations of species are known to agree.

In each theory one can write

$$\ell_{ab}^{ij} = (1/\sqrt{2})m \, n_e \, \nu_{ab} \left[Y_{ab}^{ij} + \delta_{ab} (X_a^{ij} - Y_{aa}^{ij}) \right],$$

with m the mass of the complex ion, ν_{ab} the Braginskii collision frequency, and X_a^{ij} and Y_{ab}^{ij} dimensionless quantities of order unity which specify the diagonal and off-diagonal elements, respectively. There are also terms vanishing with the mass ratio that we do not write out since they are the same in each theory. In general, it is found that Hirshman's approximation is most accurate when the ion has a large number of charge states, all with comparable values of the parameter $n_a e_a^2$. The accuracy of each X and Y is then within 20%. For general charge distributions, in fact, the off-diagonal elements Y are reliable within this margin. The accuracy of the diagonal

¹ S. P. Hirshman, Phys. Fluids 20, 589 (1977).

² C. D. Boley, E. M. Gelbard and S. P. Hirshman, "Disparate Clump Approximation in Neoclassical Plasma Transport Theory," Argonne National Laboratory, ANL/FPP/TM-113 (July, 1978)

elements X is, however, a sensitive function of the charge distribution, as measured by the set of parameters

$$Z_a = (n_a e_a^2)^{-1} \sum_{b \neq a} n_b e_b^2.$$

For example, the true X_a^{11} vanishes with Z_a , while the approximate X_a^{11} goes to a nonzero limit that depends on the Z 's for the other charge states. The same discrepancy prevails for X_a^{12} . For X_a^{22} , however, the approximate value is always correct within about 30%. From these results, it would appear that Hirshman's approximation in this problem is adequate only for the special case mentioned above, in which there are many charge states and all have comparable values of $n_a e_a^2$. These considerations apply to the first iterate of his approximation; the higher iterates, which are much more complicated, have not been examined.

B. Atomic Data Studies

K. T. Cheng and Y.-K. Kim, Radiological and Environmental Research Division

1. Spectroscopic Data for Potassium-like Ions

Spectroscopic data of highly stripped ions are useful for plasma diagnosis, as well as for plasma cooling studies. To assist in the identification of spectral lines in hot plasmas, we have carried out systematic studies of the electric dipole-allowed transitions between $n = 3, 4$ and 5 states for ions in the potassium isoelectronic sequence, using relativistic Hartree-Fock wavefunctions. For potassium-like ions, there is one valence electron outside the closed $3p^6$ core and excitation of this valence electron from the $3d^2D$ ground state are expected to give strong resonance lines. In Table V-1, we give the wavelengths, the transition rates and the absorption oscillator strengths for the $3d^2D - 4p^2P$, $4f^2F$, $5p^2P$ and $5f^2F$ transitions in Fe^{7+} , Mo^{23+} , W^{55+} and Au^{60+} ions. While the resonance lines of most alkali-like ions (ions with one valence electron) come from valence transitions only, there are indications that inner-shell excitations $3p^6 3d - 3p^5 3d^2$ are important for potassium-like ions.¹ We are in the process of studying these inner-shell transitions.

¹ K. Mori, M. Otsuka and T. Kato, "Grottrian Diagrams of Highly Ionized Iron Fe VIII-Fe XXVI," Report No. IPPJ-AM-3, Research Information Center, IPPJ/Nagoya University, Nagoya, Japan.

2. Continuum Oscillator Strengths by the Relativistic Random Phase Approximation

An important atomic process in hot plasmas is the electronic recombination where free electrons are captured by the stripped ions. In this process, electrons have to "shake off" excess energy either by emitting photons

(radiative recombination), or by colliding with a third body (collisional recombination). To study radiative recombination, one has to know the continuum oscillator strengths, or equivalently, the photoionization cross sections.²

On the theoretical side, accurate results on the continuum oscillator strengths are difficult to obtain because of strong electron-electron correlation effects. Further complications arise for heavy ions where relativistic corrections also become important. In collaboration with Prof. W. R. Johnson of the University of Notre Dame, we are developing the "relativistic random phase approximation" technique that is capable of giving reliable continuum oscillator strengths for closed-shell systems. We have successfully used this method to interpret photoionization data on Xe.³ We plan to extend our study to the continuum oscillator strengths for stripped ions with closed-shell configurations.

² D. R. Bates and A. Dalgarno, in Atomic and Molecular Processes, edited by D. R. Bates (Academic Press, New York, 1962), Chapter 7.

³ W. R. Johnson and C. D. Lin, *Bull. Am. Phys. Soc.* 22, p. 1325 (1977); and W. R. Johnson and K. T. Cheng, *Phys. Review Letters* 40, p. 1167 (1978).

Table V-1. Theoretical Data on Wavelength λ , Absorption Oscillator Strength f , and Transition Rate A for K-like ions

Transition	$j - j'$	λ (Å)	f	$(10^{10} \frac{A}{s^{-1}})$	λ (Å)	f	$(10^{10} \frac{A}{s^{-1}})$
		Fe ⁷⁺			Mo ²³⁺		
3d - 4p	3/2 - 1/2	195.1	0.060	2.12	26.44	0.032	61.5
	5/2 - 3/2	194.5	0.071	1.87	26.27	0.035	50.2
3d - 4f	3/2 - 5/2	132.0	0.431	11.0	21.67	0.789	748
	5/2 - 7/2	132.3	0.413	11.8	21.83	0.758	796
3d - 5p	3/2 - 1/2	123.2	0.010	0.837	17.47	0.006	24.6
	5/2 - 3/2	123.2	0.011	0.742	17.50	0.006	20.2
3d - 5f	3/2 - 5/2	108.7	0.171	6.43	16.38	0.189	313
	5/2 - 7/2	108.9	0.163	6.88	16.47	0.180	332
		W ⁵⁵⁺			Au ⁶⁰⁺		
3d - 4p	3/2 - 1/2	5.593	0.032	1370	4.788	0.034	1990
	5/2 - 3/2	5.463	0.025	841	4.654	0.025	1150
3d - 4f	3/2 - 5/2	4.777	0.885	17200	4.067	0.886	23800
	5/2 - 7/2	4.901	0.862	18000	4.190	0.867	24700
3d - 5p	3/2 - 1/2	3.637	0.005	551	3.094	0.006	798
	5/2 - 3/2	3.652	0.005	346	3.107	0.005	476
3d - 5f	3/2 - 5/2	3.453	0.173	6470	2.932	0.172	8890
	5/2 - 7/2	3.523	0.164	6620	3.001	0.163	9030

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