Distribution Categories:
UC-20, -20a, -20b, -20c,
-20d, -20e, -20f, -20g

ANL/FPP-77-2

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FUSION POWER PROGRAM
QUARTERLY PROGRESS REPORT

April—June 1977

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FOREWORD

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

Previous quarterly reports issued were:

CTR/TM-11	January-March 1974
CTR/TM-24	April-June 1974
CTR/TM-29	July-September 1974
CTR/TM-38	October-December 1974
ANL/CTR/TM-39	January-March 1975
ANL/CTR/TM-45	April-June 1975
ANL/CTR-75-3	July-September 1975
ANL/CTR-75-5	October-December 1975
ANL/CTR-76-2	January-March 1976
ANL/CTR-76-4	April-June 1976
ANL/CTR-76-5	July-September 1976
ANL/FPP-76-6	October-December 1976
ANL/FPP-77-1	January-March 1977

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I. FUSION REACTOR MATERIALS

A. Plasma Materials Interactions

1. Surface Blistering Studies on Fusion Reactor Materials

a. Correlation Between Blister Diameter and Skin Thickness and Its Implication to Blistering Models

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

Recently M. Risch, et al.¹ and S. Picarux, et al.² have suggested that a relationship between the most probable blister diameter, D_{mp} , and blister skin thickness, t , exists in the form $D \propto t^{3/2}$. This relationship was claimed² to apply over a wide range of helium ion energies and for a large number of materials such as Be, Ti, V, Nb, Mo, Pd and stainless steel. In addition, the existence of this relationship was claimed to support a blistering model which invokes integrated lateral stresses in the ion implanted layer as the main driving force for blister formation. Unfortunately, the suggested relationship is based on limited data on all materials except Nb. For the latter material, the information available from various authors (including some of our earlier work) has been of insufficient accuracy to allow a satisfactory determination of D_{mp} .

We have completed a systematic study of the correlation between blister diameter and blister skin thickness for helium ion irradiation of monocrystalline and polycrystalline V and Nb for ion energies ranging from 20 to 500 keV. The results indicate that the suggested relationship $D \propto t^{3/2}$ does not exist for various V and Nb targets studied. For example, for polycrystalline vanadium the following relationship between D and t is found, $D_{mp} = 6.3 t^{0.85}$ (see Figure I-1). The exponent 0.85 is significantly smaller than the 1.5 suggested in Reference 2. For polycrystalline Nb, a relationship $D_{mp} = 11.3 t^{1.35}$ was found. These relationships cannot be explained by the stress model which predicts a $D \propto t^{3/2}$ relationship. In turn, most of the information we have gathered for surface blistering with helium ions having energies larger than 40 keV indicates the applicability of our blistering model,³ which considers the excess gas pressure build-up in the region of maximum helium concentration as the major driving force for blister formation. While we have outlined the supporting evidence for the "gas pressure model" previously,³ we have obtained recently additional experimental evidence. Under 60-keV $^4\text{He}^+$ ion irradiation of annealed V held at room temperature we observed the formation of nearly spherical blisters ("balloons") (see Figure I-2). Such "balloons" can form if the surface has been deformed by the internal excess gas pressure, but

¹ M. Risch, J. Roth, and B. M. U. Scherzer, to appear in the Proceedings of International Conference on Plasma-Wall Interactions, Jülich, West Germany, October, 1976.

² E. P. EerNisse and S. T. Picraux, J. Appl. Phys. **48**, 9 (1977).

³ S. K. Das and M. Kaminsky, in Advances in Chemistry, Series No. 158, (Amer. Chem. Soc., Washington, D.C., 1976) p. 112.

Figure I-1. A Double Logarithmic Plot of Blister Diameter, D vs. Mean Blister Skin Thickness, t , for Annealed Polycrystalline and Monocrystalline Vanadium Surfaces Irradiated with $^4\text{He}^+$ Ions.

Figure I-2. Scanning Electron Micrographs of Annealed Polycrystalline Vanadium Irradiated at Room Temperature with 60-keV ${}^4\text{He}^+$ to a Dose of 0.5 C/cm², Showing Nearly Spherical Blisters. Figure I-2(b) shows a Higher Magnification Micrograph of a Portion of the Area Shown in Figure I-2(a).

cannot be formed by elastic "buckling" due to lateral stresses alone. Further evidence in support of the gas pressure model was reported recently by J. Evans,⁴ who observed blisters on the rear surface of a thin Mo sample (surface opposite to implant surface) bombarded with helium ions. Again, this result is not compatible with the stress model. A paper is being prepared for publication.

⁴ J. Evans, Submitted to J. Nucl. Materials.

b. Non-existence of Equilibrium Surface Structure Under High Dose 100-keV He⁺ Irradiations at Moderate Surface Temperatures

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

It has recently been reported by R. Behrisch, et al.,⁵ that for 100 keV He⁺ ion irradiation of Nb to a dose of 20 C/cm² (1.2×10^{20} ions/cm²) a sponge-like, porous surface structure develops, which was considered as an "equilibrium surface condition". Since the authors apparently made no effort to control the target temperature, the power deposition of the incident beam ("beam heating") caused the target to reach the high temperature of $\sim 1000^\circ\text{C}$. We have discovered^{6,7} for identical high dose irradiations but for lower surface temperatures of about 450°C that such porous surface structures are not formed, but that instead serious surface flaking occurs (in many areas more than 3 skins of $\sim 0.3 \mu\text{m}$ thickness each have been lost) without indication of an established "equilibrium surface structure" (see Figure I-3). It should be pointed out that the appearance of sponge-like surfaces has been reported by other authors and us⁸⁻¹¹ for helium bombardment of materials such as V, Ni, Nb and Mo for high target temperatures ($T \gtrsim 0.5 T_m$, where T_m is the melting temperature) and for doses which were more than a factor of twenty lower than those reported by R. Behrisch, et al.⁵

Therefore, the appearance of sponge-like surfaces of 100-keV helium irradiated metals should not be considered a high dose effect, but an effect caused primarily by the high target temperature. Furthermore, the observed serious "blister-flaking" for high dose helium irradiations of Nb at target temperatures of 450°C suggests the absence of an equilibrium surface structure and continued surface erosion with increasing dose.

⁵ R. Behrisch, et al., Proceedings of Ninth Symposium on Fusion Technology, Garmisch, Germany, Pergamon Press, 1976, p. 531.

⁶ M. Kaminsky, Proceedings of Seventh International Conference on Atomic Collisions in Solids, Moscow, U.S.S.R., September 19-23, 1977.

⁷ M. Kaminsky and S. K. Das, paper in preparation.

⁸ M. Kaminsky and S. K. Das, Proceedings of Fourth Conference on Scientific and Industrial Applications of Small Accelerators, Denton, Texas, 1976. Conference #76CH1175-9, (IEEE, New York), p. 238.

⁹ G. J. Thomas and W. Bauer, in Applications of Ion Beams to Metals, S. T. Picraux, E. P. EerNisse and F. L. Vook (eds.) Plenum Publ. Corp. 1976, p. 533.

¹⁰ S. K. Erents and G. M. McCracken, Rad. Eff. 18, 191 (1973).

¹¹ K. L. Wilson and G. J. Thomas, J. Nucl. Mat. 63, 266 (1976).

Figure I-3. Scanning Electron Micrographs of Annealed Polycrystalline Niobium Irradiated at 370°C with 100-keV $^4\text{He}^+$ Ions to a Dose of 20 C/cm² (1.2×10^{20} ions/cm²). Figure I-3(a) Shows the Entire Bombarded Spot and Figure I-3(b) is a Higher Magnification Micrograph Showing at Least Three Exfoliated Blister Skin Layers.

c. Temperature Dependence of Helium Blistering in Metals

S. K. Das, M. K. Sinha* and M. Kaminsky, Physics Division

The studies on the temperature dependence of blister formation in (110) and (111) monocrystalline nickel targets bombarded with 500-keV $^4\text{He}^+$ ions under channeling conditions have been completed (for preliminary report on these studies see ANL/CTR-76-5 p. 1). In this study the target temperature was varied from room temperature to 950°C at temperature intervals smaller than those previously used for other metals. The results show a decrease in average blister diameter with increase in target temperature (see Table I-1). The largest exfoliation of the blisters observed in the present experiments for 500-keV $^4\text{He}^+$ ion irradiation of nickel occurs at $\sim 300^\circ\text{C}$ ($0.33 T_m$) and a porous surface structure is observed at 900°C ($0.68 T_m$), where T_m is the melting point in K. These observations are in agreement with our earlier results on 500-keV He^+ ion irradiation polycrystalline vanadium¹² where the largest exfoliation was observed at 500°C ($0.35 T_m$) and a porous structure at 1200°C ($0.68 T_m$). Comparing these results also with earlier results on polycrystalline Mo, Nb, Al, and stainless steel as summarized in Table I-2, one can say that, in general, the maximum in blister exfoliation appears to occur near $\sim 0.35 T_m$ and porous surface structure is observed at temperatures above $0.5 T_m$. The appearance of porous structure depends not only on target temperature but also on other parameters, such as dose and projectile energy. The larger the implant depth, the higher is the target temperature at which porous structure is observed as seen for Al in Table I-2. At sufficiently high temperatures ($> 0.5 T_m$) enough thermal vacancies are available for the growth of the helium bubbles so that they can grow to larger sizes without building up excess pressure. When these bubbles intersect the surface, a porous surface structure is observed. A paper based on these results is being submitted for publication in J. Appl. Phys.

* North Dakota State University, Fargo, North Dakota.

¹² M. Kaminsky and S. K. Das, "Scientific and Industrial Applications of Small Accelerators," Fourth Conference 1976 (IEEE, New York), p. 238.

Table I-1. Effect of Temperature on Blister Size and Density in Nickel Monocrystals

Irradiation Temp. ($^\circ\text{C}$)	Average Blister Diameter (μm)	Average Blister Density (blisters/ cm^2)
Room Temperature	1600	1.6×10^2
500	800	2.5×10^3
600	27	5.2×10^4
700	15	2.1×10^5
850	12	4.3×10^5

Table 1-2. Homologous Temperatures at Which the Largest Blister Exfoliation and the Formation of Porous Surface Structure are Observed for ${}^4\text{He}^+$ Ion Irradiation of Metals.

Metals and Alloys	Nb	V	Mo		Al			Type 304 Stainless Steel	Ni
(He^+ ion energy, keV)	(300)	(500)	(36)	(300)	(20)	(300)	(300)	(500)	(500)
Homologous Temperature (T/T_m) at which largest blister exfoliation is observed	0.25-0.32	0.26-0.35	~ 0.28	0.23-0.3	~ 0.28	~ 0.32	~ 0.32	0.34-0.43	0.33-0.44
Homologous Temperature at which porous surface structure is observed	0.47-0.54	$>0.54-0.68$	~ 0.55	> 0.51	0.72-0.83	> 0.42	> 0.83	----	~ 0.68

2. TFTR Design Support Activities

a. Surface Damage and Erosion of TFTR Beam Dump Materials by Energetic D⁺ - Ion Irradiation

M. Kaminsky, S. K. Das, and P. Dusza, Physics Division

Several tokamak reactor designs require additional plasma heating by neutral beam injection. Since the injected neutral beam is only partially trapped in the plasma, a beam dump is needed. For TFTR, the neutral beam injector beam dump area may comprise ~ 11% of the total first wall area, and the fluxes of 120-keV D⁰, 60 keV D⁰, and 40-keV D⁰ beams reaching one of the beam dump areas, are rather high if one assumes a 10% transmission of 120-keV D⁰ component through the plasma (20-keV D⁰: 1.2×10^{16} , 60-keV D⁰: 2.9×10^{15} , 40-keV D⁰: 1.7×10^{15} ions cm⁻² s⁻¹ for an ~ 0.6 s pulse). In our experiments, the total doses for 40-, 60-, and 120-keV D⁺ irradiations of Mo were 4.2×10^{18} , 8.2×10^{18} and 2.3×10^{19} ions cm⁻², respectively, corresponding to about 2500 pulses of 1 s length. Following a request by Princeton University, the target surface temperature was not controlled (in order to study the effect of beam heating of the target) but was monitored. Preliminary results show blister formation for both the pulsed and the continuous irradiation with 40- and 60-keV D⁺ ions of Mo, but not for the continuous irradiation with 120 keV D⁺ ions, even though the total dose was higher for this case. The higher target temperature reached in the 120-keV D⁺ ion irradiation (~ 420°C) is thought to enhance the D - permeation through the lattice and to prevent gas blister build-up. The influence of target temperature and dose on blister formation is being investigated in more detail, and these studies will be extended to TZM, graphite and other candidate materials.

3. Analysis of Samples Bombarded In-Situ in PLT

a. Auger Analysis of Deposits on a Si-collector Exposed to 6-month Irradiations with Princeton's PLT

S. K. Lam and M. Kaminsky, Physics Division

We received from Dr. S. A. Cohen, Princeton University, a Si-collector which had been exposed to PLT irradiations for a six month period (July to December, 1976). Figure I-4a shows a typical spectrum of a Si-collector area which had been exposed to the plasma radiations. One can readily notice the C, O, Fe and W contamination, which is actually so severe that it obscures greatly the Si-peaks typical for the collector material. Figure I-4b shows a typical spectrum of the same collector area after it had been "sputter cleaned" by 5-keV Argon ions for 260 minutes. One can readily see that the sputter cleaning has greatly reduced the C, O, Fe and W contamination, and that the Si-peaks have become the dominant peaks. Figure I-4c shows a spectrum of a Si-collector area which had been shielded from the plasma exposure by steel strips. One can detect C- and O-contamination but not Fe and W contamination. Figure I-5 shows a sputter profile of the C, O, Fe and W contaminants and of Si substrate surface layers. One can notice that the oxygen concentration shows significant variations during the 260 minute sputtering period, while the C, Fe and W concentrations show in general a decrease in concentration as the sputtering time increases. This result suggests that

Figure I-4. (a) Auger Spectrum of Si-collector After Exposure to 6 Months Irradiations with Princeton's PLT; (b) Auger Spectrum of the Same Area of Si-collector as Shown Under (a) but After 260 Minute Irradiations with 5 keV Argon Ions (sputter cleaning process); (c) Auger Spectrum of Si-collector Area which had been Shielded from Exposure to Plasma Radiations. The Si-collector was Supplied Through Courtesy of Dr. S. A. Cohen, PPPL, Princeton University.

Figure I-5. Sputter Profiling of C, O, Fe and W Deposits on a Si-Collector, Using Auger Spectroscopy for Detection. Here, the same Si-collector Area was Investigated as the One Shown in Figure I-4(a) (before sputtering) and Figure I-4(b) (after sputtering).

at different times during the six month exposures to the PLT plasma, different amounts of oxygen have been adsorbed. From the areas under the curves a crude estimate of the Fe and W contamination concentration (based on Nb on Si calibration experiments) has been made, and an equivalent of ~ 20 monolayers of iron and 12 monolayers of tungsten has been determined. It should be pointed out that Dr. Cohen observed for other PLT irradiation periods (some of them six months' length) Fe and W deposits on Si which were sometimes more than ten times as thick as those reported here. Differences in the PLT operating conditions may be responsible for the differences observed.

4. Chemical Sputtering Effects

a. Matrix Isolation of Sputtered Zr and Be

J. Bates, D. M. Gruen and J. Tylicki, Chemistry Division

Further work has been done in the area of matrix isolation spectroscopy (MIS) which has proven to be an effective method for studying sputtering processes. The absorption spectrum of Zr has been identified in Ar, Kr and Xe matrices at 14°K. In addition, the diatomic molecule ZrN has been produced and isolated in Ar matrices. In Ar the prominent spectral features of Zr are shifted roughly 1500 cm^{-1} from the known gas phase data. In addition, some work has been done on the chemically similar element Hf where a shift of about 1000 cm^{-1} occurs in Ar matrices. The matrix isolated spectra of these elements and compounds are important for identification purposes in the application of the MIS method as well as to the elucidation of the physical processes involved.

Preliminary studies have been done on the element Be which is of intrinsic interest because of its relatively small atomic weight. MIS studies show that the element sputters with moderate ease when low mass projectiles such as H_2 and D_2 are used, unlike the situation for heavier metals. Quantitative studies of this effect are in progress. Using a hollow cathode technique, the molecular species BeH, BeD and BeO have been observed in gas phase emission but efforts to trap these molecules in Ar matrices have so far proved unsuccessful.

b. Secondary Photon Emission During Ion Bombardment of Be

R. B. Wright and D. M. Gruen, Chemistry Division

Ion bombardment induced secondary photon emission (SCANIR) from Be using H_1^+ , H_2^+ , H_3^+ , D_1^+ , D_2^+ , D_3^+ , He^+ , and Ar^+ beams of 200 eV to 15 keV energy has been investigated. Atomic spectra from BeI (neutral) and BeII (singly positively charged ions) excited species were observed with each of the above listed ion beams. The intensity of the observed atomic lines was found to be proportional to the current density of the incident ion beam; the magnitude of the intensities increasing as $\text{He}^+ < \text{H}^+ < \text{D}^+ < \text{Ar}^+$ at all energies studied. The relative intensities of the BeI and BeII lines varied with the incident beam ion and ion energy as well as the partial pressure of O_2 and N_2 in the target chamber during bombardment. These results will be used to study the excitation and de-excitation mechanisms involved in the sputtering of excited species.

c. Ion Nitriding and Secondary Ion Mass Spectrometry

Ming-Biann Liu and D. M. Gruen, Chemistry Division

Ion nitriding is a commercial process which has been used to harden metal surfaces in a glow-discharge of a nitrogen-hydrogen gas mixture at a pressure of 1-10 torr. The objective of this work is to use the ion nitriding technique to prepare metal nitrides and subsequently to characterize these products.

An apparatus was set up to carry out the preparation of metal nitrides, and is shown in Figure I-6a. Figure I-6b is the detailed drawing of the ion nitriding chamber. The sample disk was mounted on a stainless steel (or nickel) holder and the anode was a stainless steel (or nickel) disk. A mixture of 20% N₂ and 80% H₂ was prepared and stored in the 5000 ml Pyrex glass bulb. The background pressure was usually at 5×10^{-6} torr and the pressure of the reactive gas mixture was at 20-40 torr which is higher than that used by the conventional method. A dc potential of 360-450 V and a discharge current of 500-600 ma was produced. During the period of nitriding, the temperature was about 900°C.

Titanium, zirconium, niobium, and beryllium have been nitrided using the present apparatus. An energy-dispersive X-ray spectrometer has been used to identify the nitride phases. In addition, the cross section of the nitrided titanium disk was examined using a metallograph, and a thin film of the nitrided titanium was prepared by jet polishing and examined by electron diffraction. For titanium, zirconium, and niobium, metal mononitride formed. For beryllium, the discharge was hard to start, the nitrided surface was black and no characteristic X-ray diffraction peak was detected.

A secondary ion mass spectrometer is being prepared for installation on the Duoplasmatron target chamber. The UTI model 100C quadrupole is being converted to SIMS use by the addition of a cylindrical mirror energy filter and an electrostatic lens, shown in Figure I-7. It is intended to perform simultaneous SIMS and SCANIIR measurements and so obtain information about excitation and ionization processes during sputtering.

d. Primary Energy and Angle of Emission Phenomena in Secondary Ion Emission

A. R. Krauss and D. M. Gruen, Chemistry Division

A comparison has been made between the properties of the total (ion and neutral) sputtering yield and the secondary ion yield of Al, K and Ti surfaces. The results indicate substantial differences in the two emission processes. Sputtering is generally believed to be the result of momentum transfer from an energetic primary particle to a surface atom by means of a random collision cascade (RCC) process within the target. Although the sputtering yield is adequately described by this model, some of the details of the energy distribution of sputtered atoms are at variance with the predicted behavior, indicating that some emission is also caused by direct and deflected surface recoils and focused collision cascades.¹³ These effects however, generally account for less than 10% of the total sputtering.

¹³ M. W. Thompson, Phil. Mag. 18 (1968) p. 377.

Figure I-6. Ion Nitriding Chamber.

Figure I-7. Design of the SIMS Energy Filter.

The random collision cascade model predicts an energy distribution which peaks at a rather low energy E_b/n and has a high energy tail which falls off as E^{-n} , where E_b is the surface binding energy (~ 1 -eV) and $1 < n < 2$. Although the intensity of emission is expected to depend on the primary energy and angle of emission, the shape of the distribution is predicted to be independent of these quantities. A fraction of the total emission may be ionized and the secondary ion energy distribution may be represented as $N^+(E) = R^+(E)N(E)$ where $R^+(E)$ is termed the ionization coefficient. This quantity is usually increasing with energy and causes the ion energy distribution peak to occur at a higher energy, and the tail to fall off more slowly.

Because of the relation between the kinetic energy and charge state of the sputtered particle represented by $R^+(E)$, high energy, non-RCC processes such as surface recoils and focused collisions account for a sizable, and in some cases, the dominant contribution to the secondary ion emission. Figure I-8 shows the energy distribution of sputtered Al^+ , K^+ and Ti^+ . Whereas the Al^+ and K^+ energies are largely as expected, the Ti^+ distribution peaks at a rather high energy (~ 25 eV) indicating that $R^+(E)$ increases rapidly with energy. Consequently, surface recoils which produce high energy emission are responsible for a large part of the secondary ion yield, resulting in the "bump" on the high energy tail: Secondary ions with energies of 100-1500 eV account for 68% of the observed Ti^+ secondary yield. The transport of such high energy ions across magnetic field lines must of course be considered in an overall impurity transport calculation for tokamaks.

Non-RCC processes also show up in the low energy behavior of secondary ion emission. Figure I-9 shows the Al^+ energy distribution for sputtering by 500, 1500 and 2500 eV argon. The position of the peak shifts from 6 to 20 eV with increasing primary energy, indicating the effect of very short collision chains which retain some of the momentum and energy information of the primary particle.

Figure I-10 shows the energy distributions of Al^+ for various angles of emission with respect to the sample normal. At high angles of emission, the secondary ions spend a longer time in the region close to the surface and the slower secondaries will therefore have a very high probability of neutralization by charge exchange with the surface. Consequently, high energy particles are again emphasized in the secondary ion energy distribution.

e. Secondary Ion Yields of Oxidized Ti Surfaces

A. R. Krauss and D. M. Gruen, Chemistry Division

Secondary ion emission is much more influenced by processes leading to sputtering of high energy particles than is the total (ion and neutral) yield. These processes (short or focused collision cascades, and direct or deflected surface recoils) are minimized for emission along the sample normal and are usually suppressed for analytical purposes using secondary ion mass spectroscopy (SIMS). For MFE applications however, it is necessary to know the energies and secondary ion yields for emission both along and away from the target normal. High energy ions must therefore be measured and the secondary ion yields determined by integrating the energy distribution from 0 eV to a substantial fraction of the primary energy.

Figure I-8. Secondary Ion Energy Distributions for K^+ , Al^+ and Ti^+ Sputtered by 2.8 keV Argon Ions. The Lines Indicating Slopes Corresponding to E^{-1} and E^{-2} are the Limiting Slopes for the Total (ion & neutral) Energy Distribution in the Absence of Channeling and Surface Recoil Effects. $\phi_e = 55^\circ$, $\phi_i = 65^\circ\text{C}$.

Figure I-9. Secondary Ion Energy Distribution of Al^+ Sputtered by 500, 1500 and 2500 eV Argon Ions.

$$\phi_e = 55^\circ, \phi_i = 65^\circ.$$

Figure I-10. Secondary Ion Energy Distributions of Al^+ Sputtered by 500 eV Argon Ions for Various Angles of Emission. The Curves are Identified by the Angle Between the Sample Normal and the Axis of the Energy Analyzer.

The equipment used has been described in a previous report and elsewhere.¹⁴ Currently, aluminum and titanium surfaces are being examined. The Al^+ energy distributions indicate relatively little high energy contribution to the secondary ion yield whereas for Ti^+ emission, the high energy processes may actually dominate. The literature value of $S(\text{Al}^+)$ has therefore been taken as a calibration point in the measurement of $S(\text{Ti}^+)$ for polycrystalline Ti surfaces of varying oxidation. For the sake of comparison, the geometry for the Ti^+ emission has been chosen to minimize the high energy emission. This geometry will be varied once calibration has been established.

The energy distribution of Ti^+ and TiO^+ species are shown in Figure I-11. The Ti^+ peak occurs at a higher energy than that of TiO^+ and the high energy tail extends much further. This behavior is typical of all atomic-molecular oxide species studied to date and implies an agglomeration process in the sputtering of molecular species.¹⁵ The areas under the energy distributions are electronically integrated and compared with the area of the Al^+ distribution. The oxygen concentration is monitored by Auger electron and X-ray photo emission spectroscopy. The results are shown in Figure I-12 for Ar^+ and O_2^+ bombardment. The values for $S(\text{Ti}^+)$ are in agreement with literature values but the $S(\text{TiO}^+)/S(\text{Ti}^+)$ ratio is substantially smaller. It is believed that this discrepancy is due to the fact that we have measured the entire energy distribution whereas the literature value is based on the first 19-20 eV and ignores the high energy "tail" of the Ti^+ , which in fact accounts for the bulk of the emission.

¹⁴D. M. Gruen and A. R. Krauss, ANL/CTR-76-2; Applied Physics (in press).

¹⁵G. P. Können, A. Tip and A. E. deVries, Rad. Eff. 26 (1975) p. 23.

5. Modeling of Surface Behavior

a. Physical Sputtering Model for Fusion Reactor First-Wall Materials

D. L. Smith, Materials Science Division

Physical sputtering of first-wall materials by high-energy plasma particles is expected to be a major source of plasma contamination in fusion-power reactors. As a result, the achievement of satisfactory plasma performance will be dependent, to a large extent, on the proper selection of the first-wall material. However, selection of the appropriate first-wall material is hindered by a lack of data for the physical sputter yields of potential first-wall materials and by uncertainties in the energies of particles incident on the first wall. Sputter-yield data are particularly sparse for candidate low-Z materials, and the energy dependence of the yields has been experimentally determined for only a few materials. Also, the majority of the data has been obtained at incident-particle energies above 1 keV. Although the mean plasma temperature in a tokamak reactor will be of the order of 10 keV, the edge temperature and the mean-incident-particle energy are expected to be substantially lower; probably in the range of 50 to 1000 eV. Although a comparison of theoretically predicted yields with available experimental data has met with some success for heavy ions at relatively high energies (> 1 keV), the agreement with experimental data for light ions and low energies is, in general, not good.

Figure I-11. Secondary Ion Distribution of Ti^+ and TiO^+ Sputtered by 2 keV Argon Ions. Angle of Emission (ϕ_e) = 45° .

Figure I-12. Secondary Ion Yield and Oxide to Metal Secondary Ion Ratio as a Function of Surface Oxygen Concentration on Ti.

In the present investigation, a model has been developed to provide energy-dependent physical sputter-yield data for plasma particles of interest (ions and neutrals) incident on candidate first-wall materials. The expression for the physical sputter yield is based on both theoretical and experimental considerations. The general shapes of the energy-dependent sputter-yield curves are based on theoretical models, whereas the magnitudes of the yields are derived primarily from experimental data. The semiempirical model includes both high- and low-Z wall materials. The equation obtained in the present investigation for the physical sputter yield is given by

$$S = \frac{20}{U_0} \cdot Z_1^2 Z_2^2 \cdot \frac{M_1}{M_2} \cdot \frac{E}{(E + 50 Z_1 Z_2)^2}$$

where S is the atoms per incident ion; Z_1 , Z_2 and M_1 , M_2 are the atomic and mass numbers of the projectile and target atom, respectively; and the incident particle energy E and the surface binding energy U_0 are both in electron volts. The characteristics of sputter-yield curves obtained from this equation include (1) a direct energy dependence at low incident-particle energies; (2) a $1/E$ dependence at high incident-particle energies; (3) a peak at intermediate energies such that the sputter yield is relatively insensitive to energy for a moderate spectrum; (4) peaks for light ions ($M_1 \leq 4$) typically in the range of 0.1 to 10 keV; (5) heavy-ion ($M \geq 20$) sputter yields that increase directly with energy to energies of ~ 10 keV; and (6) empirically determined peak yields that generally agree with available light-ion sputter-yield data.

Figures I-13 and I-14 show the calculated energy-dependent physical sputter-yield curves for monoenergetic deuterium incident on several candidate

Figure I-13. Calculated Energy-Dependent Physical Sputter Yields of Candidate First-Wall Materials Bombarded with Monoenergetic Deuterium Ions.

Figure I-14. Calculated Energy-Dependent Physical Sputter Yields of Compound First-Wall Materials Bombarded with Monoenergetic Deuterium Ions.

wall materials. Figure I-15 is a plot of the calculated sputter-yield curves for selected particles incident on niobium. Both the shape and magnitude of the calculated curves are in fairly good agreement with available experimental data.

6. Ion Migration in Glow Discharge Tube Cathodes

William Primak, Solid State Science Division

In the proceeding Quarterly Progress Report the observation of electromigration on ion bombardment was described. It was reported that on bombardment with 140 keV ions at elevated temperatures, several silicate glasses formed surface layers of a depth exceeding the range of the ions and proportional to the incident charge. These layers were attributed to the migration of a quantity of alkali equivalent to the incident charge; i.e., that the ion current was being conducted to the metal support by ion migration in the solid.

To confirm this interpretation, a low voltage glow discharge tube in which the glass specimen could be made a heated cathode was constructed. Experiments were performed with two glasses: Pyrex glass (Corning 7740) and C-1 (Hayward Scientific Glass, Crown, $H_d = 1.523$, density 2.59 g/cm^3 , V-58.5). The Pyrex glass was studied at temperatures from 375 to 425°C; most of the C-1 experiments were performed at 400°C. The surfaces of these specimens were not uniformly affected; rather, a pattern of circular, centrally-located monochromatic fringes usually developed. The location of the fringes was plotted with a scanning optical microscope, and their order determined by the wavelength

Figure I-15. Plot of Calculated Energy-Dependent Physical Sputter Yield Curves for Niobium Showing Comparison with Available Experimental Data

variation of a particular fringes. From these data, the volume of the affected zone could be calculated. The volume and depths depended on the charge incident on the cathode. The depths of the Pyrex varied from 1.5 to 4.9 μ ; for the C-1 from 0.7 to 2.2 μ , in these experiments. The refractive index of the affected zone decreased from 1.472 to 1.462 for Pyrex and from 1.52 to 1.485 for C-1 glass.

When the cathode assembly was removed from the tube and examined, discoloration was found on the stainless steel surface in contact with the specimen, and a corresponding discoloration (brownish) was present on the glass. In one case (C-1, 76.7 mC) droplets were seen on the back of the specimen; and, under the microscope, tiny gas bubbles were rising in them. This was attributed to sodium hydroxide deliquescing and sodium reacting with the condensed water. The discoloration could be removed from the glass with detergent in an ultrasonic cleaner. An area of increased reflectivity, and in many cases a rough surface containing holes and discharge patterns, remained. Some of the specimens hummed during discharge, especially when the current was increased to 140-300 μ A, and these showed more severe surface damage.

Glow discharge usually began at about 1500 V. Then, as the experiment continued, it was necessary to raise the voltage in order to maintain the desired current. It is assumed this was caused by the high resistance of the surface layer depleted of alkali.

The glow discharge acts as a blocking electrode for the glass. Since the sodium ion migrates from the surface, there must be a corresponding migration of a negative species toward the surface or else some compensating electrode reaction. What does in fact happen is not yet determined. Among the possibilities are oxygen release, a reaction involving OH in the glass, some kind of charge storage phenomenon, or color center production. It was hoped that evidence of the negative conductor would be found, but none has been observed in these experiments as yet.

The volume of the affected zone is found to be proportional to the incident charge. Also, it corresponds approximately to the volume containing the alkali equivalent to the incident charge, calculated on the basis of typical analyses for these glasses. The similarity of these results and those obtained with ion bombardment confirms that the affected regions are ion-depleted zones formed by electrolysis. The present results also emphasize the seriousness of such processes both because they affect the whole thickness of the materials and because of the precipitates formed at interfaces.

B. Alloy Development for Irradiation Performance

1. Alloy Development Goals

S. D. Harkness, M. Abdou, S. Majumdar and B. Misra, Fusion Power Program

The commercialization of magnetic fusion energy will require the use of materials in an environment for which no appropriate data base currently exists. A study to quantify the requirements of a lithium-cooled reactor system in the important areas of creep-fatigue, thermal stress, swelling and the economic implications of increasing wall loadings and wall lifetimes has been conducted at ANL.

a. Creep-Fatigue and Thermal Stress Requirements

The ANL analysis of creep-fatigue requirements was based on a self-consistent treatment of a lithium-cooled reactor system containing cylindrical first wall cells. Detailed thermal-hydraulic calculations were used as a basis for defining the stress history to be expected at the first wall for different material choices and reactor operating conditions. The imposed duty cycle expected for different reactor operating conditions were then compared with unirradiated materials properties for Type 316 stainless steel and a vanadium alloy in order to better define whether major improvements were necessary. In addition, the results of this analysis were used to identify those reactor operating conditions where the thermal stress exceeded the yield stress of the material.

Important variables included in this self-consistent study were the end geometry and amount of restraint associated with the first wall module, the maximum temperature allowable in a system representative of either an austenitic stainless steel or a vanadium alloy, the wall loading, the burn time, the presence or absence of stress relaxation and the maximum magnetic field. The cell wall thickness was set at 3 mm based on the results of previous work that minimized the primary stresses induced by the pressure of the coolant and the thermal stresses developed through the wall.

The thermal-hydraulic calculation combined a pressure loss calculation that integrated along the coolant channel with a two-step heat transfer analysis. In the two-step heat transfer study, a one-dimensional steady state calculation was used to establish the coolant velocity and the general thermal characteristics of the system while a three-dimensional analysis was reserved for the end of the cylindrical blanket module in order to define the temperature gradients developed in the wall at different times during the burn cycle.

Since the lithium flow pattern at the first wall boundary layer is a major uncertainty, two widely different heat transfer coefficients were assumed in order to determine the sensitivity of the temperature gradient to this parameter.

The temperature gradients determined to result from different sets of operating conditions were converted to thermal stresses for a cylindrical blanket module that contained a hemispherical end cap making a variable angle of α with the side wall. The stress levels developed as function of time during the burn cycle were calculated for cases where the end cap was restrained from radial displacement but free to rotate and where it was completely clamped. The latter case was found to be more favorable and was assumed as the reference case.

The stress-time histories were calculated for two different burn cycles and included both the presence and absence of stress relaxation. The resulting stress histories were evaluated using a linear damage rule in terms of known unirradiated creep-rupture and fatigue results for Type 316 stainless steel and a vanadium alloy.

In summary, our results indicate that if the thermal stress is assumed to be limited to the yield strength, then designs employing stainless steels may be restricted to a maximum wall loading of less than 2 MW/m^2 unless a divertor is used. The same model results in a limit of 8 MW/m^2 for vanadium alloys. If the current uncertainty in the heat transfer coefficient at the first wall is taken into account, these allowable neutron wall loadings would be further reduced.

The creep-fatigue analysis underlined, among many things the strong need for in-pile measurements of this property. Using unirradiated properties as a guideline, stainless steels appear to be marginal, especially for short burn cycles in their ability to achieve the minimal 10 MW-yr/m^2 life while no problems are expected for vanadium alloys. If stress relaxation is included in the analysis, then stainless steel properties are adequate to

achieve a 10 MW-yr/m^2 lifetime at wall loadings less than 2.4 MW/m^2 , even for short (1 minute) burn cycles if they are as good in-pile as out (Figures I-16 and I-17).

In contrast, vanadium alloys appear capable of achieving 10 MW-yr/m^2 for maximum metal temperatures up to 750°C and for wall loadings as high as 4.5 MW/m^2 , even when stress relaxation effects are not considered. The incorporation of stress relaxation into the analysis shows that creep-fatigue

Figure I-16. Variation of Fatigue Life for a Clamped Stainless Steel First Wall with Wall Loading, Assuming No Stress Relaxation to Occur During Burn Time.

Figure I-17. Variation of Fatigue Life for a Clamped Stainless Steel First Wall with Wall Loading Assuming Stress Relaxation to Occur During Burn Time.

should not be a limiting consideration for vanadium-based systems unless irradiation significantly reduces their creep-fatigue properties from un-irradiated levels (Figure I-18).

The creep-fatigue analysis also comes to the important conclusion that pinned or clamped modules had longer lives than did free standing designs and that the semiangle made by the cap with the module wall was not important for these clamped designs (Figure I-19).

b. Swelling, Wall Loading and Wall Lifetime Goals

The ANL system studies approach to the economic impact of increasing wall loadings and lifetimes included a parameter space that allowed thermal output to range between 3000 and 9000 MW, aspect ratios between 2.5 and 4.0,

Figure I-18. Variation of Fatigue Life for a Clamped Vanadium Alloy First Wall With Wall Loading.

Figure I-19. Variation of Fatigue Life with Semiangle of the First Wall for the Reference Burn Cycle.

maximum toroidal magnetic fields of 6 to 14 Tesla and values of β_t from 0.06 to 0.10. A general conclusion from this study was that for a given wall loading the cost of electricity always decreased with increasing wall lifetimes (see Figure IV-2, Chapter IV).

The most economically attractive wall loading was a rather sensitive function of the available wall lifetime. Clearly the most economic systems were those in which wall lifetimes were sufficient to allow the realization of the advantages of higher wall loading in increasing the energy production per unit volume of plant. Based on the results of this study, it was deemed essential to find materials that would allow wall loads of 2.5 MW/m² for integrated lifetimes of 10 MW/m² and large economic incentives for the development of materials that allowed wall loadings of 6 MW/m² for integrated lifetimes of 30 MW-yr/m².

It should be noted in evaluating the economic impact of a change in materials properties that a savings of 1 mil/KW hr corresponds to an annual savings of nearly eight million dollars for each 1000 MWe power plant.

The impact of swelling on the economics of tokamak design was determined only by evaluating the economic penalty associated with leaving empty volume in the initial design to accommodate different levels of expected dimensional change. This is clearly an oversimplification to a complex subject since the effects of differential swelling or module replacement may be the governing consideration. However, based on leaving room in the design to accommodate swelling, the conclusion is reached that a level of 10% is the maximum that can be allowed before significant economic penalties must be paid. If this swelling limit is coupled with the suggested goal lifetime of 30 MW-yr/m² (~ 300 dpa in stainless steel) the need for a low swelling material becomes obvious.

C. Dosimetry and Damage Analysis

1. Neutron Dosimetry

R. R. Heinrich and L. R. Greenwood, Chemical Engineering Division

The neutron environment in terms of flux and energy distribution, inside the Materials Science Division's (MSD) ²³⁵U converter has been determined by the foil activation technique. The converter is being used as source of neutrons for the study of material sputtering mechanisms in a fission neutron environment. These studies are carried out at the ANL CP-5 reactor and utilize the thermal neutrons to induce fission within the converter. The converter fits into a vertical hole in the reactor and is cooled by means of a liquid helium cryostat during an irradiation.

Dosimeter capsules containing both cadmium-covered and uncovered foils were located at 7.1 cm (Position O) and 19.2 cm (Position X) from the bottom of the cryostat, which covers the area used for MSD sputtering experiments. For each of the capsules, approximately 20 foil reactions were obtained and these, along with a calculated VT-53 neutron spectrum, were used as input to the SAND-II program for iterative unfolding of the neutron spectra. The neutron energy covered by the 20 foil reactions used in obtaining the solution spectra ranged from 1×10^{-9} to 15 MeV. The spectral distribution for the O-position along with the 90% foil energy response limits is illustrated in Figure I-20 and summarized in Table I-3. The spectral distribution for the X-position was virtually identical in shape but possessed about 13% fewer neutrons than the O-position spectrum.

Table I-3. Neutron Flux Summary for MSD VT-53 Converter for O- and X-Positions

	Total	Neutron Flux, n/cm ² -sec		
		>0.1 MeV	>0.5 MeV	>1 MeV
O-Position	3.56×10^{12}	2.20×10^{12}	1.17×10^{12}	7.34×10^{12}
X-Position	3.10×10^{12}	1.92×10^{12}	1.03×10^{12}	6.53×10^{12}

Figure I-20. Measured Neutron Distribution for the MSD VT-53 Converter (0-Position)

An assessment of the uncertainties in the SAND-II solution spectra for the VT-53 converter was made by means of our error analysis code SANDANL. Results of this study for the 0-position are shown in Figure I-21 where the solid curve represents the mean solution spectrum and the dotted and dashed-line curves, respectively, represent the 1σ -plus-and-minus deviations from the mean. Although the absolute total flux is difficult to determine accurately, the error analysis does indicate that the flux above 100 keV is accurate to $\pm 20\%$, the flux above 500 keV is accurate to $\pm 10\%$, and the flux above 1 MeV is accurate to $\pm 6\%$. The uncertainty on the total flux is estimated to be $\pm 20\%$.

2. Damage Analysis

L. R. Greenwood and R. R. Heinrich, Chemical Engineering Division

The computer code SPECTRE was used to calculate spectrum averaged PKA distributions for the experimentally measured VT-53 spectra. These distributions contain 100 energy groups ranging in energy from 1×10^{-9} to 15 MeV. PKA's calculated for Al, Fe, Ni, Cu, Ag, Nb, Au, and Mn for the 0-position spectrum are presented in Table I-4 as normalized distributions over seven energy ranges. The PKA distribution for the X-position, although not given, was nearly identical to the 0-position distribution.

Table I-4. Normalized PKA Distributions (%) for the MSD VT-53 Converter (0-Position)

<u>Group, E, keV</u>	<u>Aluminum</u>	<u>Iron</u>	<u>Nickel</u>	<u>Copper</u>
0-0.1	1.8	7.3	11.4	9.9
0.1-1	9.9	22.2	41.1	35.5
1-5	24.4	35.7	28.0	30.6
5-10	15.5	11.4	7.2	8.5
10-50	40.2	20.0	10.7	14.0
50-100	3.4	1.6	0.8	0.9
>100	4.8	1.8	0.8	0.6
	<u>Niobium</u>	<u>Silver</u>	<u>Gold</u>	<u>Manganese</u>
0-0.1	7.3	9.8	15.4	25.1
0.1-1	24.5	29.9	37.5	45.5
1-5	38.1	37.8	34.6	17.6
5-10	13.7	10.6	5.6	4.2
10-50	15.7	11.4	6.8	6.5
50-100	0.5	0.4	0.08	0.6
>100	0.2	0.1	0.02	0.5

Figure I-21. Error Analysis of Experimentally Measured MSD VT-53 Converter Spectrum (0-Position)

D. Development of Barriers to Tritium Migration in Fusion Devices and Reactors

E. H. Van Deventer, V. A. MacLaren,^{*} and V. A. Maroni, Chemical Engineering Division

Recent emphasis within the permeation barrier development program has been directed towards (1) an upgrade of the hydrogen permeation apparatus, (2) completion of metallographic examinations for some previously studied permeation samples, and (3) participation in the preparation of research and development guidelines that cover requirements in the area of materials for tritium service in fusion reactors and that will eventually become part of the overall DMFE Fusion Materials Program Plan. Progress during the third quarter of FY 1977 on activities (1) and (2) is summarized below. The outcrop of activity (3) will appear in an extensive planning document being produced by ERDA/DMFE.

1. Permeation Studies

Modifications to the all-metal, high-vacuum, hydrogen permeation apparatus¹⁶ have been completed. These modifications include addition to the upstream compartment of a mass spectrometer, a low-range capacitance manometer (to 10^{-5} torr), and a multipurpose bed which can be operated either as a hot getter trap or a cold trap to collect non-metallic impurities such as H_2O , O_2 , N_2 , etc. Initial operating experience with the upstream mass spectrometer has indicated that the upstream hydrogen environment is considerably cleaner than was anticipated for hydrogen driving pressures ≥ 1 torr. A sample of 321-SS is presently undergoing investigation over a wide range of temperatures and upstream pressures primarily to test the effectiveness of the upgraded apparatus.

A cursory study of the hydrogen permeability of rhenium was attempted during the third quarter of FY 1977 in order to determine whether there would be any potential utility for this material as a permeation barrier constituent in advanced fusion reactor concepts. Although the rhenium membrane developed a leak after about two weeks of study, the initial data points indicated a permeability comparable to 300-series stainless steels. The permeability of rhenium, a group VII B element, is definitely higher than that of the neighboring group VI B element tungsten.¹⁷ Based on these limited permeation results and the fact that rhenium is a rare and costly material, there seems to be little justification for consideration of rhenium or rhenium-base alloys as permeation resistant construction materials.

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- ^{*} Undergraduate research program participant from North Dakota State University.
- ¹⁶ E. H. Van Deventer, "Hydrogen Permeability of Haynes Alloy-188", J. Nucl. Mater. 66, 325 (1977).
- ¹⁷ R. W. Webb, Permeation of Hydrogen Through Metals, USAEC Report NAA-SR-10462 (1965).

2. Metallographic Analysis of Permeation Samples

During the third quarter of FY 1977, substantial progress¹⁸ has been made in establishing procedures for the metallographic analysis of permeation specimens and in catching up on examinations of previously studied permeation samples. Methods currently being employed include conventional microscopy to characterize surface and edge morphology, electron microprobe analysis to determine elemental distributions, and scanning electron microscopy (where necessary) to provide details of the surface and edge microstructure. Both the studied permeation sample and a specimen of the as-received material are subjected to identical analytical treatment in all cases.

Results of metallographic examinations of several permeation samples investigated over the past year have uncovered a number of interesting effects. The observation of excessive surface roughness and deep running microcracks on a copper permeation sample offered a plausible explanation for the unexpectedly high hydrogen permeation rates (compared to literature values) that had been obtained with this sample. Analysis of a recently studied sample of Haynes-188 revealed a smooth uniform exterior with little evidence of elemental reorganization near the surfaces or in the bulk. These findings are consistent with the stable long-term permeation behavior observed for this particular sample.¹⁶

Electron microprobe examinations of the aluminum modified 400-series stainless steel described in the previous progress report¹⁹ have reaffirmed that heat treatment in air results in a fairly uniform buildup of aluminum near the surfaces of the alloy. Results of a series of laboratory tests showed that the rate of concentration of aluminum near the surfaces increases with both time of exposure and temperature. It is presently believed that this persistent migration of aluminum to the surface is also a function of surface oxidation potential and is the principal reason why the aluminum modified steel has demonstrated greater permeation barrier effectiveness and resiliency than previously studied aluminum coated steels.¹⁹

¹⁸V. A. MacLaren, Characterization of Tritium Permeation Barriers for Fusion Reactors: Computer Studies and Metallographic Analyses, Argonne National Laboratory Report (in press).

¹⁹W. M. Stacey, Jr., et al., Fusion Power Program Quarterly Progress Report - January-March, 1977, Argonne National Laboratory, ANL/FPP-77-1, (1977).

II. EXPERIMENTAL POWER REACTOR

A. Energy Storage and Transfer Program

R. Fuja, R. L. Kustom, R. P. Smith, R. Wehrle, EPR Project

The energy storage and transfer systems that have been proposed for TEPR¹ and described in the previous FPP Quarterly Report are under experimental assessment and construction.

An inductor-converter (IC) bridge² was built along with two superconducting solenoid magnets for modeling of the EF coil power supply. Tests on this system show experimental results agree closely with predicted theory. Measurements indicate that at least 95% of the energy, in the storage coil at the start of the transfer cycle, transferred to the load coil. The energy transfer process is reversible and the energy in the load coil has been returned to the storage coil. Polaroid pictures (Figure II-1) show the load voltage waveforms at various times in the transfer cycle.

Studies for the control of energy transfer are continuing with the objectives of developing a digital controller that will allow for the transfer and return of energy at a controlled and variable rate.

Algorithms which predict the unbalances which occur when the energy transfer rate is varied, and a method for correcting these unbalances has been developed. Work is starting on the hardware implementation for these algorithms. Protective circuits for IC bridge failures are also being studied. Circuit design to automatically make up the transfer losses will be studied and a digital controller incorporating all of these ideas will be developed.

A model homopolar generator (Hope 1), used for energy reversal in the OH coil of TEPR is under construction. Mapping of the magnetic field between the pole tips has been completed and the data reveals unbalances in the magnetic field along the ϕ and Z axis, of a cylindrical coordinate system. Methods for the elimination of these irregularities have been proposed. They include selective shimming of the pole tips and the installation of corrective coils near the pole tips. Computer studies and preparation for experimental evaluation of these ideas are under way.

The construction of the cylindrical energy storage rotor, with its air support bearings and brush support structure has been completed and mounted to the magnet's central support yoke. Optical circuits for measuring rotor growth in the radial direction are being assembled. When storing 40 kJ of energy and rotating at 2340 radians per second the radial growth of the rotor is expected to be 0.010 inches or approximately 2.54×10^{-4} meters. The effects of this growth on the operation of the air bearings will also be measured and studied.

¹ W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Conceptual Design", ANL/CTR-76-3 (1976).

² R. L. Kustom, et al., "The Use of Multiphase Inductor-Converter Bridges as Actively Controlled Power Supplies for Tokamak EF Coils," ANL/FPP/TM-78, (1976).

(1) 9% Energy Transfer - 0.5 s after start.

(3) 50% Energy Transfer - 2.0 s after start.

(2) 30% Energy Transfer - 1.0 s after start.

(4) 95% Energy Transfer - 3.3 s after start.

Figure II-1. Load Coil Voltages - 100 v/cm 0.2 ms/cm, Phase Shift 90 Degrees Leading.

Switching circuits which will connect Hope 1 to its load, a 13.6 mh inductor representing the OH coil for TEPR, have been designed, constructed and installed.

Low field, low voltage operation of Hope 1 will begin shortly. However, high voltage operation will have to wait until the field irregularities are minimized.

B. Thermomechanical Analyses

B. Misra and C. K. Youngdahl, EPR Project

The current design concept for the ANL/EPR blanket system consists of an assembly of modular stainless steel blocks with gun-bored coolant channels. The thermal hydraulic analysis is based on a single modular block that is 0.28-m thick and 1.5-m in length in a plane parallel to the first wall. To simplify the geometric model, co-radial groups of coolant channels with graded cross-sectional areas (to compensate for the exponential attenuation of neutrons in the blanket block) were selected for the initial analysis. A computer code capable of solving a set of three-dimensional thermal hydraulic equations was used to establish the transient and quasi-steady state temperature distribution within the blanket block. A set of six coolant channels with iteratively adjusted cross-sectional areas was evaluated during the first round of analyses. The neutron power profile was provided by neutronic calculations for the ANL/EPR based on a 65-s burn pulse followed by a 15-s dwell period between pulses.

The results obtained for three different coolant fluids (i.e. pressurized water, helium, and steam) are summarized in Table II-1. The operating conditions indicated in the table are consistent with those for existing pressurized water reactors, gas cooled reactors and fossil powered steam plants. Because of significant differences in the physical and transport properties of the three coolant systems, the coolant velocities, Reynolds numbers and heat transfer coefficient for steam result in a need for shorter inter-channel spacing in the direction normal to the first wall. Hence, for a given reactor, cooling with steam appears to require more coolant channels per unit volume of blanket than either water or helium. Since the length of the coolant channels is only 1.5 m, the frictional pressure drop across the channel turns out to be negligible for all cases. This indicates that one can minimize the large variations in required fluid velocity by judicious selection of coolant channel cross sectional area and can thereby optimize the heat transfer characteristics.

From the standpoint of maximum thermodynamic efficiency, it seems most advantageous to use a direct Rankine cycle with steam and a closed loop Brayton cycle with helium. However, considerations of tritium containment in direct cycle systems may ultimately force the use of intermediate heat exchangers for the near-term power plants, thus reducing the achievable power conversion efficiency. In order to estimate the magnitude of this reduction, efficiencies were calculated for steam and helium with and without intermediate heat exchangers. Significant improvement in efficiency for steam cooling can be achieved with the direct cycle approach (see Table II-1).

Table II-1. Summary of Thermal Hydraulic and Power Cycle Analyses

Structural Material: Stainless Steel

Neutron Wall Loading: 0.62 MW/m^2

Parameter/Coolant	Water	Helium	Steam
Pressure, MPa	15.17	5.17	4.83
Interchannel Distance, cm	5	5	3
Inlet Temperature, °C	240	205	260
Outlet Temperature, °C	340	400	410
Maximum Blanket Temperature, °C	500	550	550
Velocity, m/s	0.06-0.18	12-26	2-5
Reynolds Number $\times 10^{-3}$	3-13	10-30	12-50
Heat Transfer Coefficient, $\text{W/m}^2\cdot\text{K}$	1200-2550	1100-1900	400-700
Pressure Drop, MPa	< 0.01	< 0.01	< 0.01
Thermodynamic Cycle Efficiency, %			
Direct Cycle	--	13	39
Indirect Cycle	34	36	36

The very low efficiency indicated in Table II-1 for direct cycle helium is a result of the fact that the 400°C exit temperature is far below the threshold temperature for useful operation of helium turbines. From this study, it may be concluded that all three coolants can be used for near-term power producing fusion devices. Since direct cycle steam appears to offer somewhat higher efficiencies than water and helium in temperature limited systems, it merits more serious consideration as a potential power cycle concept for fusion power plants.

Mechanical response analyses for the blanket blocks shown in Figure II-2 were carried out based on the temperature distributions obtained from the above described thermal hydraulic analyses. The resulting thermal stress distribution has local regions of stress concentration at the coolant channels and at other especially hot or cold spots in the block. These stress concentrations are highly sensitive to channel arrangement; small changes in the channel pattern can result in significant alterations of the associated stress levels without affecting the thermal performance of the blanket.

A simple stress analysis procedure was developed to evaluate the relative merits of various coolant channel configurations at the conceptual design

Figure II-2. Coolant Channel Arrangement for Stainless Steel Blanket Blocks Cooled with Water, Helium or Steam.

level without resorting to the detailed and expensive stress analysis required for a final design. The solution to this procedure is based on separation of the three-dimensional temperature distribution obtained in the thermal hydraulic analysis described previously into two component distributions which respectively produce the overall deformation effects and the localized stress-concentration effects. A linear (in three-dimensional Cartesian coordinates) temperature distribution is fitted to the given temperature distribution by a least-squares technique. The corresponding deformation field is obtained exactly in closed form and represents the global deformation of the block; the associated stress field is determined by interaction with the support structure since a linear temperature distribution produces no self-equilibrated thermal stress field. Then, a residual temperature distribution is computed as the difference between the given distribution and the least-squares fit. An approximate self-equilibrated thermal stress distribution is obtained for this residual problem. The entire procedure has been automated in a small computer program which accepts the output data from the thermal hydraulic analysis. Comparisons between the relative merits of coolant channel patterns can thus be obtained rapidly and inexpensively.

As an example, thermal-hydraulic analysis using pressurized water coolant (see Figure II-3) indicated that channels spaced 5 cm apart in the toroidal direction and located at radial positions of 1, 4, 8.9, 13.8, 18.8, and 23.8 cm from the inner surface would provide a reasonably uniform temperature distribution in the block; the channels varied in cross-sectional area between 1 cm² and 0.5 cm². The associated maximum thermal strains in the block were found by the approximate procedure described above to be 0.35%. Moving the first channel to 1.25 cm from the surface reduced the strain to 0.25%. Moving the first channel to 1.5 cm and the second channel to 4.5 cm further reduced the maximum strain to 0.18%. The improvements in strain levels were accomplished without any significant change in thermal-hydraulic performance. The results indicate that preliminary thermal-mechanical optimization of blanket block design can be accomplished at the conceptual design level using relatively simple computational methods.

The overall unconstrained thermal deformation is 0.6% of the room-temperature dimensions, and the blocks become convex toward the plasma. These overall dimensional changes are virtually independent of the channel pattern variations discussed above. Stress analyses performed for temperature distributions at several times during the burn cycle indicate that the large thermal inertia of the blanket prevents any significant cyclic strain variations.

C. A Steam Dual-Cycle Power Conversion System for the EPR

H. Stevens, EPR Project

Based on the results of the thermal hydraulic analysis of the EPR blanket³ and the associated structural analysis (see Section B above). A dual cycle superheated steam system is proposed for EPR power conversion. This system which utilizes reactor superheating blanket modules delivers superheated steam directly to the turbine with a potential gross conversion efficiency of 39%.

³ W. M. Stacey, Jr., et al., Fusion Power Program Quarterly Progress Report, January-March, 1977, ANL/FPP-77-1, (1977).

Figure II-3. Thermal Strain Distribution in Radial Direction for Various Channel Arrangements.

The dual cycle is made up of an indirect pressurized water system coupled with a direct cycle superheat system. The pressurized water is used to deliver heat for evaporation from the vertical inner and outer reactor blanket to a steam generator in a closed system operating at 13.8 MPa (2000 psi) utilizing much the same conditions and components found in LWR-PWR type technology. The superheat system utilizes the steam generated on the secondary side of the pressurized water loop to feed into the reactor upper and lower superheat blanket which then feeds directly into the turbine at 8.6 MPa (1250 psig) 420°C. This circuit is comparable to the boiling water reactors in the sense that modest amounts of radioactivity will be introduced into the turbine system.

A flow diagram is shown in Figure II-4 depicting several of the major features and conditions. The system as shown is cursory pending optimization of the thermal-hydraulics and satisfactory stress analysis. Anticipated favorable conversion efficiencies with the stainless steel blanket limited to 550°C peak temperature may go as high as 40%. Blanket material optimization with steam as a coolant may substantiate even higher efficiencies for the EPR and demo plants.

D. EPR Tritium Facility Scoping Studies

R. G. Clemmer and V. A. Maroni, EPR Project

The technical basis for the design of the ANL/EPR tritium handling systems has been described in previous publications.^{1,4,5} In essence, these systems must provide for (1) recycle and processing of the plasma exhaust, (2) recovery and consolidation of tritiated wastes, and (3) emergency air handling and detritiation of the reactor building and other major facility enclosures. An important attribute of the design is provision for multiple levels of containment around all components and hardware that contact sizeable quantities of tritium.

A layout of the tritium handling and fuel processing facility for a near-term tokamak reactor like the EPR is illustrated schematically in Figure II-5, and key parameters are summarized in Table II-2. Unburned DT fuel plus impurities collected by the torus evacuation system are transferred to a consolidation manifold. The gas mixture is then compressed to 0.1 MPa by a series of oilless metal bellows pumps. The fuel is purified by trapping the condensible impurities at 30 K. The trapped impurities are processed to recover residual tritium by hot gettering or by a series of cryochemical separations.⁶ Helium is removed by a stripping column. The purified fuel is

⁴ W. M. Stacey, Jr., et al., "EPR-77: Revised Design for the Tokamak Experimental Power Reactor," Argonne National Laboratory, ANL/FPP/TM-77 (1977).

⁵ B. Misra and V. A. Maroni, "Isotopic Enrichment of Plasma Exhausts from Controlled Thermonuclear Reactors by Cryogenic Distillation," Nucl. Tech., 40 (1977).

⁶ J. L. Anderson and R. H. Sherman, "Tritium Systems Test Assembly: Design for Major Tritium Device Fabrication Review," Los Alamos Scientific Laboratory Report LA-6855-P (1977).

Figure II-4. Schematic of the EPR Coolant System.

Figure II-5. Fuel Recycle Scenario for an EPR-Type Reactor

Table II-2. Tritium Facility and Vacuum System Parameters for the FY-1977 ANL/EPK

isotopically enriched by a cryogenic distillation cascade,⁵ then prepared for delivery to the torus as ambient gas, frozen pellets, or neutral beams.

Every effort is made to minimize the likelihood and consequences of leaks. The tritium handling components and hardware are enclosed in gloveboxes or are jacketed in pipe casings. Further, the tritium processing components indicated in Figure II-5, and the emergency air detritiation systems are located in a separate reinforced containment building. The jacketed pipes and the gloveboxes are purged with an inert gas stream which is processed by a tritium waste treatment system of the type described in Reference 6.

The total tritium inventory is ~ 1300 g. Of this amount, only the 130 g that is present in the vacuum pumps and the fuel preparation units during normal operation could conceivably be released to the facility buildings (volume = 6×10^4 m³). If this release should accidentally occur, the 50 m³/s emergency cleanup system would take about 50 hours to reduce the ambient tritium levels to the recommended radiation control guideline of 5 $\mu\text{Ci}/\text{m}^3$.

The estimated cost of the tritium handling facilities (based in part on material in Reference 6), excluding the torus evacuation systems, in 1977 dollars is $\$18 \times 10^6$. This figure includes an overall contingency of 21%. The costs are dominated by a few select items: emergency cleanup system = $\$6.3 \times 10^6 + 20\%$ contingency, waste treatment system = $\$3.2 \times 10^6 + 20\%$ contingency, gloveboxes and purifiers = $\$2.7 \times 10^6 + 25\%$ contingency. The total cost is relatively insensitive to specific reactor design parameters.

E. Vacuum Systems

J. Moenich, EPR Project

The following table reflects the watts of refrigeration required per hour to operate 80 m² of cryopanel in each injector of EPR-77. The cryopanel uses 20°K shielding between the 77°K and 4.2°K surfaces. Cooldown to operating temperature and regeneration requirements are not included in this table, but these needs will be supplied by the same refrigerant system as used during normal operation.

Table II-3. Cryopanel Refrigerant Requirements/Hour

	Watts at		Liters
	4.2°K	20°K	L/N ₂
Radiation Losses	0.1	32.0	184.0
Thermal Loads (neutron flux)*	224.0	786.0	17.6
Conduction Losses	14.0	83.0	0.4
Heat Input (from gas load)	6.0	22.0	
TOTALS/Injector	244.1	923.0	202.0

* Based on 0.001 watts/cm² of panel.

Twelve injectors will be necessary for plasma heating in EPR-77. The necessary refrigeration would be delivered by four units, each capable of delivering 1000 watts at 4.2°K and 4000 watts at 20°K, and four liquifiers, each capable of delivering 850 liters of liquid nitrogen per hour. Three refrigeration units and three liquifiers will be capable of delivering the required refrigeration with some reserve. The fourth unit will be on standby and for maintenance rotation. Estimated costs for each of the four refrigerators is 4 to 6 million dollars. The estimated cost of each liquifier is \$375,000.

Zirconium-aluminum getter panels are another means for pumping the neutral beam injectors. These will be especially attractive should the neutron flux into the injectors result in prohibitively high thermal loading on cryopumping panels. Sixty (60) m² of zirconium-aluminum panel would be required to handle the equivalent gas load. The 60 m² of panel would operate at 400°C and require ≈ 260 kW of power each hour to maintain this temperature. At the present cost of 0.0191/kW hour for operating the ZGS here at Argonne operating cost of the zirconium-aluminum panel would be $\approx$ \$5.00/hour. Once every 2 to 6 days, depending on the hydrogen equilibrium pressure, the zirconium-aluminum panel temperature must be raised to $\approx 700^\circ\text{C}$ to reactivate the panels. Assuming one hour reactivation time and a power requirement of 1440 kW, the cost of reactivating the panel each time would be $\approx$ \$28.00. These costs are based on the use of external power for heating. Thermal loading of the zirconium-aluminum panel by the neutron flux should appreciably reduce these costs. It may be necessary to use cryopumps with the zirconium-aluminum panels to eliminate gases that are either not pumped at all or are poorly pumped by the panels. These cryopumps however present, at most, 2-1/2% of the required pumping. The use of a combination of zirconium-aluminum pumps and cryopumps would result in a substantial savings in costs over the use of cryopumping alone.

III. TNS PROJECT

A. Tritium Facility Conceptual Design Study

1. Overall TNS Tritium Facility Design and Analysis

R. G. Clemmer and V. Maroni, Chemical Engineering Division and
J. M. Mintz*

Work on the design of the TNS tritium facility is progressing. Systematic analyses of fuel cycle parameters and trade studies on the sensitivity of these parameters to TNS design and operating conditions were performed. Design concepts for the various tritium handling subsystems were identified and costing algorithms based on application to specific TNS point designs were constructed. The prospect of an upgradable TNS, capable of being modified to demonstrate the feasibility of producing electrical power, provides the principal incentive for these parametric studies. The key parameter is the duty factor which varies from ~ 0.1 for initial operation to ~ 0.75 for full upgraded operation. All aspects of the fuel cycle are significantly affected, e.g., the total torus evacuation speed rises from ~ 60 to ~ 400 m³/s and the total tritium inventory from ~ 0.25 to ~ 2 kg. Also, if the reflectance coefficient (the fraction of escaping plasma particles that recycle to the plasma) is very low, as it might be if divertors are included, then torus evacuation requirements become much more demanding and throughputs and inventories rise dramatically. In the design area it is concluded that turbomolecular pumps could perform adequately on a low duty factor TNS, but cryopumping is necessary for an upgradable device. Medium vacuum tritium pumping (~ 1 to ~ 100 Pa) is identified as a development item. Hot gettering methods of chemical impurity removal are favored over pure cryogenic methods, but a combination of the two may well prove superior. Studies of air detritiation scenarios indicate a required clean up rate of $\sim 0.4\%$ of the building volume per minute with a unit cost of $\sim \$1,000/\text{cfm}$. The overall capital cost of the tritium handling systems is $\sim \$20$ M and is relatively insensitive to machine size or design.

A comprehensive report describing this work in greater detail has just been published.¹

* Industrial participant from General Atomic Company.

¹ "TNS Scoping Studies," joint report by Argonne National Laboratory and General Atomic Company, ANL/FPP/77-2 and GA-A14412 (May, 1977).

2. Multicomponent Isotopic Separation: Parameter Study and Transient Analyses

J. F. Davis** and B. Misra, Chemical Engineering Division

Shown in Figure III-1 is the proposed scheme for the TNS distillation column cascade used for the cryogenic separation of isotopomeric forms of hydrogen. The scheme incorporates four columns and one equilibrator with the spent reactor fuel

** Masters Thesis Student from Northwestern University.

fed into column #1 and the neutral beam recycle split around the equilibrator which is located between columns #1 and #2.

Results of a parameter study of the performance of this cascade are presented in Table III-1. As expected, the separations are better at the lower pressures than the higher, but the improvement at the lower pressures is small. Since the pressure effects are minimal, the more practical scheme of descending pressures through the cascade appears reasonable. Results from varying the number of theoretical plates show that the initial column has the greatest effect on the final product, especially with regard to the hydrogen-tritium separation. The amount of the neutral beam recycle fed to the equilibrator has only a slight effect on the tritium concentration in the protium waste stream.

Figure III-1. TNS Cryogenic Distillation Cascade.

Table III-1. Cascade Parameter Study^a

Case	Column/Parameter				Stream		
	I	II	III	IV	A	B	C
<u>Pressure (Torr)</u>							
1	1500	1250	1000	750	{ APH APD APT	0.04 99.3 0.59	49.6 47.8 2.9
2	1500	1500	1500	1500	{ APH APD APT	0.04 98.7 1.21	50.0 47.9 2.3
3	750	750	750	750	{ APH APD APT	0.01 99.7 0.33	50.0 48.1 2.1
<u>Number of Theoretical Plates</u>							
1	75	30	30	30	{ APH APD APT	10 ⁻³ 99.8 0.16	51.4 48.4 0.42
2	75	50	50	50	{ APH APD APT	10 ⁻³ 99.9 0.04	51.4 47.9 0.8
3	50	50	50	50	{ APH APD APT	0.01 99.9 0.06	50.9 47.2 2.1
<u>Amount of Neutral Beam Recycle to Equilibrator, NB^a</u>							
1		1.0			{ APH APD APT	0.01 99.9 0.06	50.9 47.2 2.1
2		4.0			{ APH APD APT	0.01 99.9 0.05	48.8 49.2 1.92
3		8.0			{ APH APD APT	0.01 99.9 0.04	42.3 56.3 1.31

^a See Figure III-1.

Work is progressing on the development of a computer code for studying transient effects in a packed distillation column used for separating isotopomeric forms of molecular hydrogen. Although the present TNS design requires a cascade of three to four columns to effect the desired separations, the focus of this transient analysis project is on only the lead column, since this column has proven to have the greatest impact on overall cascade performance. The model chosen is a pseudo-plate model in which the packed section of the column is divided into a specified number of sections of equal height, and the column efficiency is represented by a set of efficiencies calculated for each component in each stage. The main objectives of this project are: (1) to study the transient effects generated by a varying feed rate and feed composition, (2) to recommend a start-up procedure which minimizes the time required to establish equilibrium performance, (3) to study various scenarios for eliminating total restarts in the vent of a failure elsewhere in the processing system, (4) to study the effects of major upsets in the operation of the column (e.g., partial loss of reboiler heat), and (5) to develop and test plausible control scenarios for the distillation cascade.

B. Neutral Beam Heating

J. A. Fasolo, Accelerator Research Facilities Division

Beam transport, power flow and gas flow calculations have been done for two 60-MW, 150-keV neutral beam injector designs which may be regarded as lower and upper limits to what might be achieved with sources of positive deuterium ions. Both designs call for six injectors, with two beam lines per injector. Design 1, the "current" reference design, assumes a D^+ fraction of 75%, achieved in some present-day neutral injector ion sources, and relatively minor extrapolations in duty cycle and beam energy beyond the state of the art required for TFTR ion sources. This design will be continually updated as warranted by advances in the state of the art and/or by experimental determinations of design parameters that can only be estimated at the present time. Design 2 assumes a D^+ fraction of 95%, a major extrapolation beyond the state of the art for TFTR ion sources; this design represents a worthy but perhaps not quite attainable goal for further D^+ ion source development work. Some results of the beam transport, power flow and gas flow calculations are shown in Table III-2.

A program has been written to calculate $\int (p/q_N) dl$ through a tapered circular or square neutralizer, the gas flow q_N into the neutralizer, and the gas flow out of each end when the gas is fed into the center of the neutralizer. This completes the task, begun several months ago, of writing and debugging programs for D^+ transport, neutralizer design, gas loads, power flow and injector efficiency.

A preliminary design study of a double focusing bending magnet has been done by J. Norem. The pole pieces in this design are flat and slotted radially into a number of sectors separated by gaps which increase the open area and the conductance-limited pumping speed along the D^+ , D_2^+ and D_3^+ beam paths. Current-carrying conductors are used to obtain the required field distribution for an $n = 1/2$ magnet.

Table III-2. Abridged TNS Neutral Beam Injector Design Summaries

	Design 1*	Design 2
Neutral beam power, P_B (MW)	60	60
Neutral beam energy, U_B (keV)	150	150
Beam composition (D^+ , D_2^+ , D_3^+)	(0.75,0.18,0.07)	(0.95,0.03,0.02)
Number of injectors	6	6
Number of beamlines per injector	2	2
Number of ion sources, N_S	12	12
Ion current per source, i_+ (A)	163.4	123
Bending angle, D^+ beam, θ (deg)	72	72
Beam size at first wall (m x m)	0.390 x 0.390	0.392 x 0.392
Average beam power density at first wall (MW/m ²)	32.9	32.5
Total gas flow per injector (Torr·l/s)	252	149
Total ion beam power, P_+ (MW)	294	222
Power required to produce the ion beam, kP_+ (MW)	323.5	244
Assumed k value	1.1	1.1
Assumed direct conversion efficiency	0.85	0.85
Fraction of P_+ recovered by first convertor, P_{R1}/P_+	0.13	0.03
Fraction of P_+ recovered by second convertor, P_{R2}/P_+	0.41	0.53
Net power input to injectors, $P_{\text{supply}} = kP_+ - P_{R1} - P_{R2}$ (MW)	165	120
Electrical power conversion efficiency, $\epsilon_e = P_B/P_{\text{supply}}$	0.36	0.50

* Current reference design.

A survey has been made of the literature on stripping cross sections relevant to the production and loss of neutrals in neutral beam injectors employing direct extraction D^+ ion sources. Design studies of such injectors have begun.

C. Neutral Beam Pumping

J. Moenich, Accelerator Research Facilities Division

Table III-3 below reflects the refrigerant needs for neutral beam pumping in terms of refrigerator and liquifier capacity per injector per hour of operation. The 173 m² of cryopanel is operating at 4.2°K and is shielded on both sides by 20°K panels. The 20°K panels in turn are shielded by 77°K panels.

The thermal load due to the neutron flux and the heat input from the gas load are calculated on a 75% duty cycle. Initial operation planned for a 10% duty cycle will reduce these two values by a factor of 7.5.

Thermal loading from the neutron flux places the greatest demand upon the refrigeration. This factor alone enhances the possibilities of using zirconium-aluminum getter panels for pumping the neutral beams. Since the required 115 m² of zirconium-aluminum getter panel per injector must be operated at ~ 400°C. the thermal loading from the neutron flux should be reduce the cost to maintain this temperature. Even if all external power were used to heat the panels, the cost to operate the panels (based on 0.0191/kW per hour to operate Argonne's ZGs accelerator) would be ~ \$9.50 per hour per injector.

Table III-3. Refrigerant Needs for Neutral Beam Pumping

	Watts at		Liters
	4.2°K	20°K	L/N ₂
Radiation Losses	0.2	69.0	397.0
*Thermal Loads (from neutron flux)	454.0	1593.0	36.0
Conduction Losses	30.0	179.0	0.9
Heat Input (from gas load)	12.0	45.0	< 0.1
Totals	496.2	1886.0	434.0

* Based on 0.001 Watts/cm³.

D. Penetration Shield Analysis

J. C. Jung and M. A. Abdou, Applied Physics Division

Method developments for and nuclear analysis of major penetrations in TNS have continued. Several penetration sizes and shapes for neutral beams and vacuum pumping ducts were examined. A substantial effort was devoted to a dual purpose connected beam and evacuation duct system. In this concept, the torus evacuation ducts are connected to the neutral beam ducts at locations outside the blanket/bulk shield. This eliminates the need for separate evacuation ducts that penetrate the blanket/bulk shield with direct streaming of the plasma neutrons. The connected duct system has several advantages over the separate duct system. These include: (1) reduction in total radiation streaming through the combined duct system and (2) the fraction of the blanket volume occupied by penetrations is substantially reduced. Another possible incidental benefit is the reduction of the radiation streaming through the neutral beam ducts to the interior of the beam injectors because a fraction of the neutrons and photons escape into the evacuation side channels. Furthermore, this concept permits locating the vacuum pumping ducts at locations where the space problems of local exterior penetration shield are less severe.

A sample of the results is shown in Table III-4. Parameters for the reactor and duct system calculated are also shown in Table III-4. The computations were carried out with the continuous energy Monte Carlo code VIM with 15,000 source neutron histories. The toroidal-field coils concentrically surround the bulk shield about the toroidal magnetic axis with an inner radius of 4.3 m, an outer radius of 4.9 m and a width of 0.9 m in the present calculation. The side surface of the coils is away from the neutral beam duct center by 0.775 m. The position of the connected vacuum pumping duct axis (vertical to the midplane of the reactor) in case 2 is about 1.36 m from the end of the bulk shield and in case 3 about 2.15 m. Although a relatively large duct size of 0.75 m in diameter for both neutral beam duct and vacuum pumping duct was chosen in order to reduce the statistical variance of the solution, the problem appears adequate enough to obtain a general insight into the effect of duct connection. The numbers in parenthesis in Table III-4 represent the statistical error as estimated by VIM.

It can be seen from the results in Table III-4 for location 1 that connecting the evacuation ducts to the beam ducts results in a very substantial reduction (about a factor of 30 for the case in Table III-4) in the radiation level at the vacuum pumps. This illustrates the significant benefits obtained by using a connected dual purpose duct system. One can also note that one single bend in the duct system results in a very significant reduction in radiation streaming. Since bends in ducts are possible for rf heating but are not permitted with neutral beams, these results also show an important advantage for the rf heating from the shielding point of view.

Table III-4. Effect of Connection of Vacuum Pumping Duct
Neutron Flux ($\text{n/cm}^2\text{-s}$)

Location	Case 1 No Connection of Evacuation Duct	Case 2	Case 3
		Evacuation Duct Con- nected to Beam Duct at a Location 2.2 m Away From First Wall	Evacuation Duct Con- nected to Beam Duct at a Location 3.0 m Away From First Wall
(1) Vacuum Pump	7.56 (-7) ($\pm 20\%$)	2.70 (-8) ($\pm 15.3\%$)	2.35 (-8) ($\pm 20.4\%$)
(2) TF Coil	4.25 (-8) ($\pm 16.9\%$)	4.55 (-8) ($\pm 11.7\%$)	3.27 (-8) ($\pm 10.5\%$)
(3) NB Duct Wall Outside B/S (2.2 m from First Wall)	1.67 (-6) ($\pm 19.1\%$)	-----	1.36 (-6) ($\pm 16.6\%$)
(4) NB Duct Wall Outside B/S (3.0 m from First Wall)	4.06 (-7) ($\pm 12.8\%$)	4.63 (-7) ($\pm 15.9\%$)	-----
(5) NB Duct Wall Inside B/S	2.37 (-5) ($\pm 3.7\%$)	2.19 (-5) ($\pm 4.8\%$)	2.18 (-5) ($\pm 4.8\%$)
(6) End Cap of NB Duct	7.56 (-7) ($\pm 20.4\%$)	6.79 (-7) ($\pm 24.2\%$)	8.32 (-7) ($\pm 20.8\%$)
(7) System Leakage	2.40 (-3) ($\pm 11\%$)	2.37 (-3) ($\pm 10\%$)	2.33 (-3) ($\pm 14\%$)

Blanket/Shield Thickness = 85 cm (SS/B₄C Layers)

Penetration Shield Thickness = 20 cm (50% SS + 50% B₄C)

NB-Duct Diameter = 75 cm

NB-Duct Length = 360 cm

Vacuum-Duct Diameter = 75 cm

Vacuum-Duct Length = 100 cm

Normalization = 1 Source DT Neutron

VIM-Monte Carlo with 15,000 Histories

IV. FUSION SYSTEMS ENGINEERING

A. Fusion Reactor Systems Studies

M. A. Abdou, Applied Physics Division

During the third quarter of FY-1977, systems studies for tokamak power plants have continued in both areas of detailed subsystems analysis and global reactor parametric analysis. A trade-off study concerned with the neutron wall loading and the structure lifetime was carried out. The primary energy conversion subsystem study completed a series of analyses for a helium-cooled stagnant lithium blanket concept. The plasma subsystem analysis was continued and significant conclusions were reached. A study aimed at providing the goals for the ERDA/DMFE alloy development program was completed and a summary of the results is given in Chapter I, section B of this report.

A summary of the work carried out and sample results obtained in the systems studies is given below.

1. Wall Load and Lifetime Goals for Tokamak Power Plants

M. A. Abdou, Applied Physics Division

A parametric systems study was carried out to investigate the impact of major design parameters and the performance characteristics of reactor components on the economics of tokamak power plants. Of particular importance is the performance of the first-wall and the structural material in the blanket. The following is a summary of the trade-offs concerned with the neutron wall loading and the structure lifetime. The results provide a useful input to determining the goals for structural alloy development.

The neutron wall loading, P_w , is strongly related to the reactor thermal power, P_{th} , the surface area of the first wall, A_w , the plasma power density, P_p , and the plasma volume. The motive for a higher P_w capability is that it makes it possible to design higher power density, smaller size and potentially more economical reactors. However, there are upper limits, on the highest wall load that can be realized, arising from: (a) physics constraints on the power density achievable in the plasma; and (b) limitations unique to tokamaks on the smallness of the reactor size. There are also limits on the usability of a high P_w dictated by the structure cooling capability and its lifetime in a harsh radiation environment.

An extensive parameter survey was made to determine the range of wall loads producible in tokamaks. A sample of the results is given in Figure IV-1 which shows P_w as a function of the maximum toroidal field, B_m , for a reactor with $P_{th} = 3000$ MW(th). The results are shown at four values of plasma average β_t : 0.04, 0.06, 0.08 and 0.1 for an aspect ratio of 4 and plasma D-shape elongation, κ , of 1.3. P_w varies from 1 to 9 MW/m² in the range $6 < B_m < 13$ T and $0.06 < \beta_t < 0.1$. Other systems studies results indicate that the optimum magnetic field is ~ 9 T. At $B_m = 9$ T, P_w varies from ~ 3 MW/m² at $\beta_t = 0.06$ to $P_w \sim 5$ MW/m² at $\beta_t = 0.1$. For a larger power

Figure IV-1. Neutron Wall Load as a Function of Toroidal-Magnetic Field Strength and Plasma β_t . ($P_{th} = 3000 \text{ MW(t)}$, $A = 4$, $\kappa = 1.3$ and inner blanket/shield thickness = 1 m).

reactor with $P_{th} = 7000$ MW(th), P_w at $B_m = 9$ T varies from ~ 5 MW/m² at $\beta_t = 0.06$ to ~ 8 MW/m² at $\beta_t = 0.1$. The producible P_w is sensitive to the aspect ratio, A . For $B_m = 9$ T, $\beta_t = 0.08$, and $\kappa = 1.3$ increasing A from 3 to 4 increases P_w from ~ 3 MW/m² to ~ 4 MW/m² for $P_{th} = 3000$ MW(th). The wall load decreases slightly as the plasma elongation, κ , is increased from 1 to 1.65. P_w is particularly sensitive to β_t and P_{th} . The uncertainties in the plasma stability limit on β_t and in the projected optimum plant power rating at the time fusion is commercialized complicates the task of determining a target wall load for structural alloy development.

The ANL general systems computer program, which can parameterize the performance and economics of the entire tokamak power plant, was employed to examine the economic trade-offs pertinent to the wall load and structure lifetime. A sample of the results is given in Figure IV-2 which shows the cost of energy in mills/kWh as a function of neutron wall load (MW/m²) and structure lifetime (MW-yr/m²). The cost of energy includes return on capital cost, operation and maintenance cost and a prorated first-wall/blanket replacement cost. The results in Figure IV-2 are based on total cumulative downtime for wall/blanket replacement of 160 days. It is clear from Figure IV-2 that the cost of energy always decreases as the structure lifetime increases but the wall load at which the cost of energy is minimum depends on the structure lifetime.

If the structure lifetime is limited to ≤ 5 MW-yr/m² then the optimum wall load is ~ 4 MW/m². Increasing the structure lifetime makes it economically attractive to operate at higher wall loads. For lifetimes > 13 MW-yr/m², better economics are obtainable by operating tokamaks at higher wall loads up to $P_w \sim 8$ MW/m² provided that efficient cooling of the first wall and blanket structure is feasible. For these relatively long lifetimes (> 13 MW-yr/m²), the economic gain per unit increase in the wall load is large up to $P_w \sim 4$ MW/m² and becomes smaller at higher P_w . At any given wall load a large reduction ($\sim 30\%$) in the cost of energy is achieved by increasing the lifetime from 5 to 13 MW-yr/m². The reduction in the cost of energy obtainable by further increase in the structure lifetime is smaller but is still significant.

Based on the results of this study, goals for structural alloy development can be recommended regarding the wall load capability, calendar lifetime (years) and fluence lifetime (MW-yr/m²). The goals are classified into two categories: (a) very important (Priority 1); and (b) important (Priority 2). Achieving the goals in Priority 1 category ensures that the structural materials do not pose appreciable limitations on the economic competitiveness of tokamak power plants. Accomplishing the goals in the Priority 2 category will provide an important step in a comprehensive research and development program to improve the utilization of the tokamak potential as a relatively inexpensive energy source. The Priority 1 goals are 3 MW/m², 4 yr and 12 MW-yr/m². The Priority 2 goals are 5 MW/m², 6 yr and 30 MW-yr/m². These results are based on our present understanding of tokamaks. Future results, experience and the burden of economic competitiveness may require appreciable modification of these goals. For example, material resources limitations, long-term radioactive inventory problems and benefits of simpler designs obtainable with relaxation of requirements on frequency of shutdown and length of downtime may demonstrate a more pressing need for longer structure lifetime.

Figure IV-2. Cost of Energy as a Function of Neutron Wall Load, P_w , and Structure Lifetime ($A = 3$, $\kappa = 1.65$, $\beta_t = 0.14$ and maximum structure temperature = 650°C).

2. Thermal Hydraulic Analyses of Lithium Blanket Concepts

B. Misra and V. A. Maroni, Chemical Engineering Division

A series of thermal hydraulic analyses were completed for a helium-cooled stagnant lithium blanket concept. A primary objective of this study was to make the coolant operating parameters and characteristics reasonably consistent with existing HTGR experience. Table IV-1 contains a summary of results with stainless steel as construction material and with the following sets of conditions: (1) the presence of or absence of a divertor (assuming that the heat flux to the first wall is 25% of the neutron wall loading without a divertor and 10% with a divertor), (2) maximum allowable structural temperatures of 500°C or 650°C, (3) thermal stress limits of 17 or 34 ksi, and (4) heat transfer coefficient and friction factor multipliers of 2.2 and 4.0, respectively. The asterixed parameters were fixed in the calculation and the primary iteration was done by increasing the neutron wall loading until the thermal stress limit at the first wall was exceeded. The geometry being analyzed is a transverse arrangement in which the first wall is a single layer of tightly packed tubes and the blanket is in essence a sea of lithium with appropriately dispersed, helium-carrying tubes. A realistic boundary condition is set up so that there is no discontinuity in the thermal hydraulic analysis at the first wall/blanket interface.

In the thermal hydraulic analysis of the first wall the coolant velocity is fixed at 200 ft/s, the tube diameter is fixed at 25.4 mm (1 inch) and the tube wall thickness is not allowed to be less than 1.25 mm (50 mil). In the thermal hydraulic analysis of the blanket, the coolant void fraction, VF, is fixed at 5% and the temperature drop through the lithium, $\Delta T(\text{Li})$, is set at 25°C. Trade studies on the latter two parameters have been completed for VF = 5, 7 and 10% and $\Delta T(\text{Li}) = 25, 50$ and 100°C. These studies show that in terms of maximum coolant exit temperature, VF = 5% and $\Delta T(\text{Li}) = 25^\circ\text{C}$ are closer to optimum than the other values tested. Other parameters that were fixed in these analyses are given in Table IV-1. The coolant exit temperature, from the first wall region, $T(\text{CE})$, may be represented empirically in terms of the neutron wall loading, P_w , and the maximum allowable structural temperature, $T(\text{MAX})$, by:

$$T(\text{CE}) = T(\text{MAX}) - 17.9 \cdot P_w - 21.9 \text{ (with divertor)}$$

$$T(\text{CE}) = T(\text{MAX}) - 41.3 \cdot P_w - 21.4 \text{ (without divertor)}$$

The coolant exit temperature from the blanket region is nominally 30 to 50°C below the maximum structural temperature and seems to be relatively independent of both wall loading and divertor scenario.

The principal indications from this study are that: (1) $T(\text{MAX}) \geq 650^\circ\text{C}$ is essential for useful $T(\text{CE})$ with helium, i.e., $T(\text{CE}) > 500^\circ\text{C}$; (2) without a divertor P_w is limited to 2 to 5 MW/m² depending on allowable thermal stress level (ignoring, of course, the effects of fatigue and crack propagation for the time being); (3) in principal, considerably higher wall loadings can be achieved with a reasonably effective divertor; (4) fabrication of the first wall out of 50-mil tubing is assumed to be feasible (if the limit were 100 mil, the allowable P_w values would drop by a factor of 2); and (5) the number of tubes and gross tube weights for a reactor with a first wall area of $\sim 100 \text{ m}^2$ are extremely large.

Table IV-1. Trade Study for Helium Cooled Stagnant Lithium Blanket Using Transverse Cooling Channels Made of Stainless Steel: First Wall Configuration is a Single Layer of Close-Packed Tubes.

(* Indicates fixed or independent parameter)

<u>Divertor Option</u>	<u>With Divertor</u>		<u>Without Divertor</u>	
<u>General Parameters</u>				
* Maximum Structural Temp., °C [T(MAX)]	500	650	500	650
* Helium Inlet Pressure, ksi	1.0	1.0	1.0	1.0
* Pump Power/Thermal Power, %	5	5	5	5
* Coolant ΔT, °C	200	300	200	300
* Lithium ΔT, °C [T(Li)]	25	25	25	25
Neutron Wall Loading, MW/m ² [P _w]	6.2	12.4	2.6	5.1
<u>First Wall Parameters</u>				
* Helium Tube Dia., mm	25.4	25.4	25.4	25.4
* Helium Velocity, ft/s	200	200	200	200
* Thermal Stress, ksi	17	34	17	34
Wall Thickness, mm	1.25	1.25	1.25	1.25
Channel Length, m	12.2	17.3	12.2	18.4
HT. TR. Coeff., BTU/hr.ft. ² °F	1693	1510	1679	1720
Coolant Inlet Temperature, °C	171	217	173	220
Coolant Exit Temperature, °C [T(CE)]	371	405	373	410
Wall ΔT, °C	53	50	53	50
No. of Tubes, Hundreds ^a	32	22	32	22
Total Tube Weight, Tons ^a	35	35	35	35
<u>Blanket Parameters</u>				
* Void Fraction, % [VF]	5	5	5	5
Tube Diameter, mm	4.9	5.2	7.7	5.8
Wall Thickness, mm	0.41	0.42	0.48	0.43
Channel Length, m	1.1	1.5	2.3	1.8
HT. TR. Coeff. BTU/hr.ft. ² °F	2541	2361	2068	2253
Helium Velocity, ft/s	251	263	220	255
Coolant Inlet Temperature, °C	259	305	268	308
Coolant Exit Temperature, °C	459	605	468	608
Wall ΔT, °C	8.0	8.0	6.0	8.0
Thermal Stress ksi	2.7	2.6	2.0	2.5
No. of Tubes, Thousands ^a	939	585	174	398
Total Tube Weight, Tons ^a	74	71	54	66

^a Based on a reactor with a first wall area of ~ 1000 m²

$$T(CE) = T(MAX) - 17.9 P_w - 21.9$$

$$T(CE) = T(MAX) - 41.3 P_w - 21.4$$

Several other features of this study are worthy of note. (1) Whereas the coolant velocity in the first wall was fixed at 200 ft/s, in the analysis of the blanket it proved judicious to fix the coolant void fraction and as a result the blanket coolant velocity had to be maintained as a dependent variable. Nonetheless, the calculated blanket coolant velocities came out within the range from 200 to 300 ft/s. (2) To perform a truly comprehensive design analysis, the number, size and length distributions of the coolant channels should be properly graded throughout the radially adjacent sub-regions of the blanket. This type of analysis would show that the diameter and length of the tubes would increase while the coolant velocity decreased as the distance radially outward from the first wall increased. Grading was not attempted for the cases summarized in Table IV-1, wherein the blanket heat transfer analyses were set up mainly to accommodate the cooling requirements near the first wall. In reality, the VF and $\Delta T(L_i)$ values need not be constant throughout the blanket and as a result the number of blanket tubes can be reduced somewhat from the values given in Table IV-1. (3) Application of the multiplier factors for the heat transfer coefficient and frictional pressure drop is based on the assumption that the interior tube walls can be roughened or otherwise modified to augment heat transfer but not without a significant impact on the pressure drop. Omission of these multiplier factors (i.e. equating them to unity) generally results in substantial increases in coolant velocity (to > 400 ft/s) and channel length (70 to 80%) with somewhat smaller reductions in heat transfer coefficient ($\sim 30\%$) and coolant exit temperature (4 to 5%).

3. Plasma Subsystem Analysis

D. Ehst, Applied Physics Division

This quarter the plasma subsystems study achieved its initial objectives, resulting in the conclusions summarized here and detailed in reference 1.

In general we found that plasma power density in tokamak reactors can be insensitive to characteristics of the MHD equilibrium, if plasma beta is fixed, and the following trends were observed.

1. Power density and ignition, $n_i \tau$, are only slowly varying functions of α_n / α_T for a fixed pressure profile. (Here we denote the pressure, density and temperature profile widths by α , α_n and α_T , respectively; large parameter values represent narrow profiles.) We chose $\alpha_n = 0.3$, representative of a relatively broad density distribution for subsequent analysis.

¹ D. Ehst, K. Evans, Jr. and W. M. Stacey, Jr., "A Systematic Survey of Tokamak Reactor Physics Design Parameters," Argonne National Laboratory (to be published).

2. Power density and $n_1\tau$ values become more favorable for peaked pressure profiles compared to flat profiles. We set $\alpha = 1.4$ as a typical value, although future MHD stability analyses should indicate more clearly which pressure profiles are actually attainable.
3. Power density is insensitive to Z_{eff} , except that ignition becomes impossible ($n_1\tau \rightarrow \infty$) at low $\bar{T}_e$ as Z_{eff} increases. By taking $Z_{\text{eff}} = 1.7$, ignition is assured for $\bar{T}_e$ as low as 8 keV.
4. Power density tends to peak at $\bar{T}_e \approx 13 \text{ keV} (1 + \alpha_T)^{-1}$. This, plus the fact that $n_1\tau$ is slowly varying for $\bar{T}_e$ in the range of 8 to 15 keV, motivated our standard choice $\bar{T}_e = 8 \text{ keV}$.

Reactor costs are strong functions of magnetic field, major radius and blanket/shield thickness. Our choices among these parameters were influenced by the nature of the MHD equilibrium and resulted in the following observations.

5. Triangularity of the plasma cross section has only mild effects on plasma current for a fixed B and R; it has only small effects on plasma volume and consequently does not change the B versus R curve at constant reactor power. Thus, our arbitrary choice of a triangularity versus ellipticity relationship was permissible for our purposes. If high beta stability is significantly dependent on triangularity, some other sequence of triangularity values could be more pertinent.
6. Pressure profile effects are negligible on plasma current and B versus R for $1.0 \leq \alpha \leq 2.0$.
7. We saw that $\beta_{t \text{ max}}$ must increase substantially as aspect ratio decreases in order to benefit from low aspect ratio designs. This is especially true of small size, large field tokamaks.
8. For a fixed aspect ratio, the increase in reactor power with elongation is evident even if β_t does not increase with the ellipticity, κ . The high current requirements at large κ are equally apparent.
9. Large increases in plasma current are required if the safety factor is reduced; these may be compensated by increases in β_t .

One shortcoming of the present systems study is the inability to predict the optimum path of investigation through the multidimensional parameter space. For example, future stability studies may shed light on the proper relationships among A, κ , d, q and α needed to maximize β_t . It also seems plausible that the path of stability through MHD space may have indistinct boundaries since small deviations from the path may simply result in plasmas with mildly degraded confinement properties. Finally, we stress that the data base presented here is best evaluated in the context of the overall power plant parametric study in which the costs and benefits of different choices are quantitatively examined.

4. Plasma Driving System

J. Brooks, Applied Physics Division

An analysis of the driving system requirements for a typical commercial fusion reactor was completed and a report² was issued. The reactor is an 8-m major radius tokamak with a power output of 1000 MW(e). The reactor has the conventional coil configuration of a central, solenoidal OH coil and an EF coil externally located to the TF coil. Four different driving system power supply configurations have been examined as to their suitability for this reactor. The analysis used a plasma burn cycle dynamics code fully coupled to a driving system code. For each power supply configuration, a range of ohmic heating ramp times, Δt_{OH} , and neutral beam turn-on times, t_{B_0} , was simulated.

In general, feasible startup times for this reactor are about the same as for an EPR; OH reversal times of 1 to 4 s followed by a beam heating period of about 5 s. The B requirements on the OH coil are considerably less than for an EPR, basically due to the larger radius of the OH coil. The OH power and energy requirements are similar to an EPR but the EF requirements increase considerably. In general, there is a strong degree of coupling between the driving system requirements and the details of the plasma physics, particularly the temperature behavior and the MHD field requirements of the plasma.

Figure IV-3 summarizes the total driving system power supply cost as a function of the OH ramp time for the different configurations. This data has been generated by applying cost algorithms developed for the various hardware components to the technological requirements found by the burn cycle analysis. Figure IV-3 is for the case of the neutral beam turned on just after the OH ramp, generally the best operating mode. The cost of each configuration is extremely sensitive to the ramp time; this illustrates the need for a very precise and coupled analysis of the plasma and the driving system. In general, the cheapest operating point is the most cost effective.

All of the configurations studied use an SCR-type power supply for the EF system and for the auxiliary (burn-phase) OH supply but differ in the type of energy transfer device used in the OH system for startup and in the type of central energy storage device.

Configuration No. 1, which uses a homopolar generator for the OH energy transfer device and an energy storage inductor (ESI) as the central storage element, results in the smallest power supply cost: \$105 M for a Δt_{OH} of 3 s. The energy storage requirement for the homopolar generator, for this case, is $U_{OH}^{max} = 1460$ MJ, the EF power requirement is $P_{EF}^{max} = 1568$ MVA, $B_{EF}^{max} = 2.5$ T/s and the ESI storage capacity is 9460 MJ. Based on the OH flux swing used during startup, a burn time of about a half hour would be possible.

² J. N. Brooks, W. M. Stacey, Jr. and R. L. Kustom, "Plasma Driving Systems Requirements for Commercial Tokamak Fusion Reactors," Argonne National Laboratory, ANL/FPP/TM-87 (1977).

Figure IV-3. Total driving system power supply cost as a function of the OH ramp time, Δt_{OH} and power supply configuration. For the Neutral beam turn-on time, $t_{B0} = \Delta t_{OH}$.

In configuration No. 2 an SCR power supply is used instead of a homopolar generator, as the OH transfer element. At the cheapest operating point, $\Delta t_{OH} = 2.3$ s, the cost is about \$25 M more than for configuration No. 1. This is basically because the SCR supply costs more than the homopolar generator. The requirements for this case are $p_{max}^{EF} = 2100$ MVA, $p_{max}^{EF} = 1568$ MVA, $B_{OH}^{max} = 2.0$ T/s, and ESI storage = 8350 MJ.

Configuration No. 3 is the most conventional in terms of available technology. Here a motor-generator-flywheel set is used as the central energy storage device along with an SCR-type power supply for the OH transfer element. Although the requirements for this configuration, exclusive of the MGF set, are the same as for configuration No. 2, the cost is much higher and nearly double that of the cheapest operating point using a homopolar generator-ESI combination. This large additional cost is due almost entirely to the cost of the generator portion of the MGF set. Furthermore, the cost estimates for this configuration assume a voltage compatible power supply-MGF-set combination, i.e. with no transformers used. If transformers were needed the cost of configuration No. 3 would be even greater.

In configuration No. 4a a dump resistor is used as the OH energy transfer device, together with an ESI as the central storage device. Use of a dump resistor, which has an essentially trivial cost, eliminates the need for a symmetric OH current swing during startup (needed for the other transfer devices), and so permits the full design value of the OH flux swing to be used. The potential burn time is therefore increased to about one hour. The use of a dump resistor, however, requires a shorting switch in the OH circuit. If this switch must be solid state, as has been assumed, it will be expensive. Also, the range of ramp times for a single dump resistor is limited to fairly short times and this increases the EF requirements. For these reasons, configuration No. 4a is comparatively expensive, about \$185 M for the cheapest case. This may or may not be worth the doubled burn time. Requirements for this case are $B_{OH} = 3.4$ T/s, $p_{max}^{EF} = 2576$ MVA. If four resistors are used to obtain a variable resistance during startup as in configuration No. 4b, the maximum OH voltage during startup can be lowered and the ramp time increased. This saves on EF supply cost relative to configuration No. 4a but involves higher switch costs. The cheapest case for configuration No. 4b costs about the same as configuration No. 4a. The allowed burn time is also about 1 hr.; $B = 2.0$ T/s and $p_{max}^{EF} = 1568$ MVA are lower, while circuit complexity and control would be somewhat greater.

B. Development of Blanket Processing Technology for Fusion Reactors

J. R. Weston, C. J. Wierdak* and V. A. Maroni, Chemical Engineering Division

The program underway at the Argonne National Laboratory to initiate the development of processing technology for liquid lithium breeder blankets is continuing. Progress during the third quarter of FY-1977 is summarized below.

* Student Aid from Illinois Institute of Technology.

1. Lithium Processing Test Loop (LPTL)

Work on the LPTL^{3,4} is progressing in good accord with the schedule. The support structures, the vessels, the flow meters and the EM pump have all been installed and the interconnecting pipe sections (received from the vendor in May, 1977) have been welded into place. Radiography of the field welds has been completed and the acceptability of all welds has been verified and documented. Tack welding of the heating cable assemblies to the vessels is nearing completion. Couplings for all of the cover gas access ports on the vessels are being fabricated and installed in preparation for evacuation and leak checking of the entire loop system. Installation of the counter weighting supports for the EM pump, flow meters and pipe sections is also nearing completion. All of the control circuits have been assembled and are presently being inserted into the electronics racks. The power bus and circuit breaker systems that will provide both primary and standby emergency power to the LPTL facility have been installed. Plans have been made to have an outside vendor prepare the 15 gallons of salt (LiF-LiCl-LiBr) required for startup of the salt tank.^{3,4} The salt is scheduled for delivery in October, 1977. Designs for the reservoir tank sampling system and the salt tank electrode assemblies have been formulated and fabrication is progressing. All LPTL related documentation and test results, including, system descriptions, fabrication specifications, radiography, drawings, operating procedures, etc., are being maintained in a permanent file for future reference. Work remaining to be done on the LPTL prior to startup includes: (1) installation of the cover gas handling systems, (2) wrapping of the vessels and pipe sections, (3) coupling of the instrumentation consoles to the loop, and (4) filling of the dump tank with lithium.

2. Support Activities

The lithium mini-test loop (LMTL) described in previous progress reports^{3,4} was shut down after ~ 6000 hours of operation for repair of an electrical short in the leak detection system and for upgrading of the trace heating lines to permit more efficient high temperature operation. Inspection of the exterior surfaces of the reservoir tank, loop, piping and welds (all 300-series stainless steel) showed no evidence of deterioration due to penetration or embrittlement. One of the two Nupro T-series bellows-sealed valves on the LMTL had withstood in excess of 200 operating cycles up to the time of shutdown. A number of lithium samples were extracted from the LMTL during the six week period prior to shutdown, and chemical analyses (similar to those reported in the preceeding progress report³) on these samples are now being completed.

Adaptation of the half-scale glass model of the salt tank to permit tests of the microprocessor control circuit is nearly finished. The microprocessor has been programmed and checked out with a test board. The gas pressure control systems have been assembled and are presently being leak checked.

³ W. M. Stacey, Jr., et al., Fusion Power Program Quarterly Progress Report - January-March, 1977, Argonne National Laboratory, ANL/FPP-77-1 (1977).

⁴ W. M. Stacey, Jr., et al., Fusion Power Program Quarterly Progress Report - October-December, 1976, Argonne National Laboratory, ANL/FPP-76-6 (1976).

C. Safety Studies of CTR Concepts

I. Charak, Reactor Analysis and Safety Division

In this quarter, the first wall thermal model was successfully used to calculate the cyclic temperature variations associated with an EPR-type burn cycle. Also, bench-scale tests were performed to assess the validity of the containment building tritium-cleanup model and to suggest adjustments to the model and/or parameters.

1. First-wall Thermal Model

As noted in the previous quarterly report,³ thermal transients on the first-wall system are being studied with the CINDA-3G code.⁵ CINDA-3G is a general purpose code which solves the diffusion equation using finite-difference techniques. It is extremely flexible in that the user has available more than 100 built-in subroutines to accomplish a great variety of calculations. In addition, the user can provide FORTRAN subroutines for added flexibility.

The modeling and calculational effort is focused on two major aspects of the system:

(a) quasi-steady-state temperature distribution under postulated burn-cycle conditions.

(b) transient temperature distribution under abnormal operating conditions, e.g., plasma dump on the first wall.

In this quarter, effort was concentrated in the first area. In particular, several wall configurations were studied as an adjunct to the ongoing ANL/EPR design effort. The results of these calculations are detailed temperature distributions, in time and space, through the first wall/vacuum wall system. These temperatures can then be used to calculate the cyclic wall stresses due to the periodic heatup-cooldown cycles associated with the burn dynamics.

An example of one such calculation is given in Figure IV-4. This case consists of a 0.5-cm thick graphite first wall (also referred to as the "liner") which radiates its heat to a 2-cm thick water-cooled stainless steel vacuum vessel. The burn was assumed to be uniform for 65 s, with a 15-s dwell time. The energy deposition corresponded to an average neutron wall loading of 1 MW/m^2 , with a commensurate radiation and transport surface flux of 0.25 MW/m^2 on the first wall. A low value for graphite thermal conductivity ($0.01 \text{ cal/cm-s-}^\circ\text{K}$) was used for conservatism. In this case, we have plotted the hottest graphite and hottest steel temperatures. Some of the main results are summarized in Table IV-2.

⁵ Chrysler Corporation, "Chrysler Improved Numerical Differencing Analyzer for Third Generation Computers," Engineering Department Technical Note, TN-AP-67-287 (October, 1967).

Figure IV-4. Peak First Wall/Vacuum Wall Temperatures.

Table IV-2. First Wall/Vacuum Wall Temperature Differences [F(C)]

	End of Burn	End of Dwell	Change Over Burn Cycle
Across liner	556 (309)	266 (148)	--
Across steel	98 (54)	83 (46)	--
Liner hot spot	--	--	479 (266)
Steel hot spot	--	--	43 (24)

Effort was begun to treat the thermal effects of a plasma dump. The model under development considers the following scenario:

- (a) At some point in the burn cycle, the plasma is disrupted,
- (b) all the stored thermal energy and some fraction (up to 1.0) of the stored magnetic energy in the plasma is dissipated on the surface of the first wall*;
- (c) the time scale for (b) will be treated as a parameter over the range currently thought to be reasonable (perhaps 100 μ s to 10 ms),
- (d) the dump area will also be treated parametrically,
- (e) the pulse heating on the first wall surface, if severe enough, will ablate the surface,
- (f) based on (e), first-wall lifetime estimates can be made based on a judgment of the frequency of dumps over a given time period. By the same token, if the first wall and the pressure boundary become a single vessel, then the potential for loss of containment can be evaluated and factored into subsequent safety analyses.

* Later refinements may treat the dissipation of magnetic energy (i.e., I^2R heating) more realistically as a volume-distributed source.

2. Analysis of Tritium Soaking Mechanisms

V. A. Maroni, R. G. Clemmer and R. H. Land, Chemical Engineering Division and J. M. Mintz*

Efforts to develop an understanding of the potential effects of various reaction and soaking mechanisms following sizeable accidental tritium releases to large enclosures (representative of projected fusion reactor buildings) have continued during the third quarter of FY-1977. In order to provide a test of the computer model for air detritiation analysis presented in previous progress reports,^{3,4} a bench-scale experiment was set up to simulate, at least in part, the conditions anticipated for large enclosures following a tritium release. The experimental assembly, diagrammed in Figure IV-5, consisted of a ~ 40 liter cubical plastic desiccating box with a flat plexiglass door clamped to one face of the box and sealed by a circumferential rubber gasket. This box (hereafter referred to as the enclosure) was attached to two separate gas circulation loops. One loop (monitoring loop) was connected directly to a flow-through type tritium monitor (Triton Model 955B) having its own internal circulation pump. The other loop (processing loop) consisted of a flow meter, an oxidizer bed (Hopcalite) operated at ~ 550°C, a moisture trapping bed (Linde 4a molecular sieve) operated at 0°C and a diaphragm-type circulation pump. Typical experiments were carried out by: (1) injecting enough tritium as HT to raise the tritium level in the enclosure to from 1000 to 3000 $\mu\text{Ci}/\text{m}^3$, (2) circulating the enclosure atmosphere through the monitoring loop until a stable tritium signal was achieved, and then (3) starting up circulation through the processing loop.

To a first order of approximation, a cleanup operation, carried out in the manner described above, should proceed with a simple exponential dependence on time, that is

$$\frac{dN_T}{dt} = \left(\frac{\epsilon Q}{V} \right) N_T \quad (1)$$

where N_T is the measured tritium concentration at any time, t , ϵ is the efficiency for removal of tritium from the process loop stream, Q is the volumetric flow rate and V is the effective volume of the enclosure. In actual practice the uncertainties in measuring Q and V (at best a few percent in each case), coupled with the fact that the value of ϵ is near unity, make it difficult to determine the extent to which equation (1) is obeyed. This is important because when the trapping efficiency is only slightly less than unity a significant amount of tritiated moisture can be transported back to the enclosure. The consequences of these uncertainties should be borne in mind when considering the results described below.

Figure IV-6 shows the results of a typical detritiation simulation on the enclosure. The apparent obedience of equation (1) persists from the starting

* Industrial participant from the General Atomic Company.

Figure IV-5. Block Diagram of the Small-Scale Air Detritiation Test Apparatus.

Figure IV-6. Results of a Typical Experiment with the Small-Scale Air De-tritiation Apparatus.

Figure IV-7. Clean-up Curves for Various Operating Modes with the Small-Scale Air Detritiation Apparatus.

point ($t = 0$, $N_T \approx 1300 \mu\text{Ci}/\text{m}^3$) to an N_T value roughly 40 times lower, after which a pronounced tailing of the cleanup curve is observed. Based on (1) the best estimates that can be made of the enclosure volume (including the Kanne chambers in the tritium monitor) and (2) the measured flow rate, the processing efficiency derived for this experiment was $\sim 96\%$. During other experiments like the one in Figure IV-6, one or the other and in some cases both of the processing steps (oxidation and water stripping) were temporarily by-passed. When both processing steps are by-passed at a point in time after the tailing has begun, the tritium level in the enclosure usually tends to rise slowly with time, sometimes increasing overall by as much as 50 to 100%. Further, most of the residual tritium that is present after tailing has been reached, can be removed from the enclosure simply by reinserting the molecular sieve bed alone. This result implies that near the end of these simulated cleanup operations a large fraction of the tritium present in the enclosure is in the oxidized form (HTO) -- perhaps the result of incomplete trapping.

Following an initial series of experiments of the type shown in Figure IV-6, an electrical feed-through was adapted to the enclosure to permit operation (inside the enclosure) of a circulation fan and a calrod heated stainless steel plate with $\sim 200 \text{ cm}^2$ of surface area. Results of clean-up operations performed on the enclosure for various sets of operating conditions involving the fan and heated plate are shown in Figure IV-7. Operation of the fan during detritiation (case B) seems to enhance the tailing effect, possibly by accelerating the rate of the reaction/adsorption/release mechanisms involving HT and more importantly HTO. Surprisingly, operation of the hot plate at $\sim 200^\circ\text{C}$ (case C) demonstrated little obvious effect on the detritiation operation relative to case B. Although the hot plate might be expected to cause an increase in the rate of HTO formation it also raises the enclosure temperature by $\sim 20^\circ\text{C}$ which would tend to reduce the amount of adsorbed HTO.

Efforts are presently underway to match the measured detritiation curves with the computer model described in earlier progress reports.^{3,4} Initial results on this task have indicated the need for considerably larger $\text{HT} + \text{H}_2\text{O} \rightleftharpoons \text{H}_2 + \text{HTO}$ rate constants and HTO sticking probabilities than the values derived and reported previously.^{3,4}

D. Cross-Section Measurements and Techniques

1. Evaluated (n;p) Cross Sections of ^{46}Ti , ^{47}Ti and ^{48}Ti

D. Smith and A. Smith, Applied Physics Division and C. Philis* and G. Bersillon*

Microscopic evaluated neutron cross sections for the reactions $^{46}\text{Ti}(n;p)^{46}\text{Sc}$, $^{47}\text{Ti}(n;p)^{47}\text{Sc}$ and $^{48}\text{Ti}(n;p)^{48}\text{Sc}$ have been obtained from threshold (or zero energy) to 20 MeV. The results have been presented in graphical and numerical (ENDF format) form. The microscopic evaluated cross sections were compared with measured fission-spectrum-averaged values.

* Bruyeres-le-Chatel.

2. Comments on the Energy-Averaged Total Neutron Cross Sections of Structural Materials in the Few MeV Range

A. Smith and J. Whalen, Applied Physics Division

Broad-resolution total neutron cross sections of carbon, iron and titanium were measured with varying sample thicknesses from ≈ 1 to 4 MeV. Energy-averaged cross sections were deduced to accuracies of ≤ 1 percent. The carbon values are consistent with previously reported precision measurements. The measured energy-averaged iron and titanium values are larger than the calculated energy average of some previously reported higher resolution measured and evaluated cross sections by as much as 10 percent. These differences may be indicative of a physical phenomenon which has implications on the design of neutronic systems.

3. Analysis of the Sensitivity of Spectrum-Average Cross Sections to Individual Characteristics of Differential Excitation Functions

Donald L. Smith, Applied Physics Division

A simple procedure for analyzing the sensitivity of computed spectrum-average cross sections to detailed properties of excitation functions for threshold nuclear reactions commonly used in reactor dosimetry is described. The approach is based upon consideration of two energy-dependent functions F and G defined by the formulas

$$F(E) = \int_0^E \sigma(\epsilon)\phi(\epsilon)d\epsilon$$

and
$$G(E) = \int_0^E \sigma'(\epsilon)\phi(\epsilon)d\epsilon,$$

where σ is the differential excitation function, σ' is its first derivative with respect to energy and ϕ is the neutron spectrum used in computing the spectrum-average cross section $\langle\sigma\rangle$. This procedure is shown to be useful in identifying specific needs for improved differential data and as a tool to be employed in performing evaluations.

4. Determination of the Energy Scale for Neutron Cross Section Measurements Employing a Monoenergetic Accelerator

J. W. Meadows, Applied Physics Division

Persistent disparities between the energy scales used for broad spectrum neutron source studies has prompted an investigation of various factors which affect the determination of an energy scale for monoenergetic accelerators.

Yield curves and neutron energy spectra have been calculated for some (p,n) reactions commonly used as neutron sources or for energy calibration purposes. These calculations take into account the energy spread of the incident proton energy loss. It is shown that when thresholds are observed by detecting the 0 deg. neutron yield the best results are obtained by plotting the square of the yield against the proton energy and extrapolating to zero yield. A linear plot can be in error by 1-2 keV if the energy spread of the proton beam is large.

5. Titanium-I; Fast Neutron Cross Section Measurements

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

Energy averaged total neutron cross sections have been measured from ≈ 1.0 to 4.5 MeV with few percent statistical accuracies. Differential elastic neutron scattering angular distributions have been measured from 1.5 to 4.0 MeV at incident neutron energy intervals of ≤ 0.2 MeV. Differential cross sections for the inelastic neutron excitation of "states" at 158 ± 26 , 891 ± 8 , 984 ± 15 , 1428 ± 39 , 1541 ± 30 , 1670 ± 80 , 2007 ± 8 , 2304 ± 22 , 2424 ± 16 and 2615 ± 10 keV were measured for incident neutron energies from 1.5 to 4.0 MeV. Additional "states" were observed at approximately 2845 and 3009 keV. An energy-averaged optical-statistical model was deduced from the measured values and the implications of its use in the context of the strong fluctuating structure has been investigated.

V. MAGNETIC SYSTEMS

A. Negative Ion Source Development

1. Charge Exchange H⁻ Ion Sources

J. A. Fasolo and H. R. Hiddleston, Accelerator Research Facilities Division

The multiaperture, multigrid extraction geometry of the 30 Hz source¹ has been temporarily replaced by a high-perveance parallel-ribbon extraction geometry in which ion beam is extracted from the source plasma through a single-aperture, 1.96-cm diameter source grid which is followed by a parallel-ribbon W-3 Re extractor grid with 107 ribbons spaced 1.78×10^{-2} cm center-to-center. This geometry is similar to that in the operational 1 Hz source but the grid diameter is larger by 30%, the accelerating gap is smaller by a factor of two and the grid ribbon spacing is 46% smaller. This geometry has produced a 30 Hz, 10 mA H⁻ beam with an extraction voltage of 7.0 kV. After preliminary tests of this geometry were completed, the recirculating D'yachkov-type supersonic sodium vapor cell,^{2,3} which has been under development for use with a modified version of the 30 Hz source, was installed between the unmodified source and the Einzel-type lens used to transport the beam to the collector. With the cell installed, the distance from the extractor grid to the collector increased from ~ 2 m to ~ 2.3 m.

Before the cell was loaded with sodium, a 5 Hz, 11 mA H⁻ beam was obtained with an extraction voltage of 7.0 kV, an indication that H⁻ beam interception by the cell was negligible. This result was duplicated with the cell loaded but cold. When the cell was heated to operating temperature, the H⁻ current⁴ decreased by a factor of two.

There are several possible explanations for this result; the one believed to be most plausible has to do with the fact that the source is a tandem acceleration source in which positive ions extracted from a hydrogen plasma are accelerated to an energy of 7 keV before entering an electron attachment cell filled by the hydrogen outflow from the plasma source. In this cell, H⁻ ions and H⁰ atoms derived from H⁺ and the fragments of dissociation of H₂⁺ and H₃⁺ are produced with energies of 7, 3.5 and 2.33 keV. The H⁻ ions acquire an additional 7.0 keV of energy in the second gap of the tandem acceleration source and emerge from the source with energies of 14.0, 10.5 and 9.33 keV. It is these higher energy H⁻ ions that are collected in the absence of sodium vapor, but when the sodium cell is in operation it is the lower energy neutrals that produce all but a negligible fraction of the H⁻ beam emerging from the sodium cell. The H⁻ accelerating field is terminated by

¹ J. A. Fasolo, IEEE Trans. on Nuclear Science, NS-24(3) (1977) p. 1597.

² H. R. Hiddleston, J. A. Fasolo, D. C. Minetti, R. E. Chrien and J. A. Fredrick, Proc. of 1976 Proton Linear Accelerator Conf., Chalk River, Ontario, Report No. AECL-5677 (1976) p. 387.

³ H. R. Hiddleston, IEEE Trans. on Nuclear Science, NS-24(3) (1977) p. 1600.

two high-transparency wire-mesh grids used to eliminate the focusing effect of the accelerating gap. Because of an increase in energy, ion beams emerge from the H^- gap with lowered divergence while the neutrals emerge with the original divergence. (Each energy component may be regarded as a separate base.) Interception of the more divergent neutrals, or H^- ions derived from them, by the sodium cell or by beam line components may account for a large fraction of the losses that have more than offset the expected gains due to higher conversion efficiencies in sodium. Inability to focus these more divergent H^- ions with the Einzel lens may account for additional losses. The expected gains, in the absence of transmission and transport losses, are given by η_{Na}/η_{H_2} , the ratio of conversion efficiencies in sodium and hydrogen targets. These expected gains and some characteristics of the particles emerging from the H^- source are given in Table V-1. In the table, α is a divergence angle.

The distance from the extractor grid to the sodium cell was ~ 41 cm in this experiment. A net gain might be achieved by shortening this distance and thus reducing transmission losses. The sodium run lasted several hours. It was terminated when a sodium leak in the line from the sodium reservoir to the boiler was noticed. Aside from this problem, the recirculating sodium cell appeared to be operating normally.

2. Direct Extraction H^- Source

J. A. Fasolo, Accelerator Research Facilities Division

A duplicate of the Los Alamos Penning-type cesium-activated surface plasma source has been built, using drawings provided by Los Alamos and preparations are being made to take the source to Los Alamos for preliminary testing. Work on the Argonne test bench is proceeding slowly, on a part-time basis.

Table V-1. Characteristics of Particles Emerging from the H^- Source and Expected Gains in Sodium When the Extraction Voltage is 7.0 kV

Origin of H^- ions	$H^+ \rightarrow H^-$	$H_2^+ \rightarrow H^-$	$H_3^+ \rightarrow H^-$
H^- energy, E^- (keV)	14.0	10.5	9.33
H^0 energy, E^0 (keV)	7.0	3.5	2.33
$(\alpha^0/\alpha^-) = (E^-/E^0)^{1/2}$	$\sqrt{2}$	$\sqrt{3}$	$\sqrt{4}$
Expected gain, η_{Na}/η_{H_2}	3.9	14.4	16.5

VI. APPLIED PLASMA PHYSICS

A. Multispecies Transport in Tokamaks

C. D. Boley, Applied Physics Division

In the last report, a program for incorporating some recent, important results of neoclassical transport theory^{1,2} into the Argonne 1-D plasma transport code was described. The specific task at hand was to cast the results into a form appropriate for immediate coding. This has now been accomplished, and all the details are given in a technical memorandum.³ One of the problems noted in the last report was the evaluation of certain energy integrals which occur in the banana-plateau transport expressions. It was found that, in the disparate-mass limit, these integrals can be expressed in terms of a single integral depending on two parameters, the collisionality and the effective charge of a species. The most practical procedure is to generate a table of values of the integral that can be referred to during the running of the 1-D code.

¹ S. P. Hirshman and D. J. Sigmar, *Phys. Fluids* 20 (1977) p. 418.

² E. M. Gelbard and S. P. Hirshman, to appear in *Phys. Fluids*.

³ C. D. Boley, "Practical Results of Neoclassical Transport Theory for a Multispecies, Mixed-Regime Plasma," Argonne National Laboratory (to be issued).

B. Atomic Data Studies

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

1. Relative Importance of Ionization and Discrete Excitation Cross Sections for Fe¹⁵⁺ and Mo³¹⁺

We have extended earlier calculations of oscillator strengths, excitation energies and the (Plane-wave) Born cross sections to higher excited states of Na, Fe¹⁵⁺ and Mo³¹⁺. This was in preparation for the application of a sum rule on the Born cross sections.⁴ By subtracting the sum of cross sections for discrete excitations from that for the total inelastic scattering, one can find the ionization cross section. Although our computation is still incomplete, we can get a rough estimate of the relative importance of the discrete excitation and ionization by comparing the leading terms of the Born cross sections, i.e., the sum of f_n/E_n , where f_n is the dipole oscillator strength and E_n is the excitation energy (in rydbergs) to the n th level (including continuum). The ratio $\sum f/E$ summed for all ionizing collisions ($\equiv M_{\text{ion}}^2$)

⁴ Y.-K. Kim and M. Inokuti, *Phys. Rev. A* 3 (1971) p. 665.

to that for all inelastic scattering ($\equiv M_{\text{tot}}^2$) indicates the relative importance of the ionization process. In Table VI-1 we list M_{tot}^2 , M_{ion}^2 and M_{disc}^2 (i.e., sum of f/E for all discrete transitions) for Na, Fe^{15+} and Mo^{31+} . The data in Table VI-1 show that though only $\sim 20\%$ of all inelastic collisions of fast electrons with neutral Na would lead to ionization, the ratio increases to $\sim 50\%$ for highly stripped ions.

2. Confirmation of Spectroscopic Data for Mo and W Ions Reported in LA-6679-MS

R. D. Cowan of Los Alamos Scientific Laboratory calculated a large volume of spectroscopic data (e.g., oscillator strengths and excitation energies) on molybdenum and tungsten ions.⁵ His data were calculated by a semi-relativistic method,⁶ with empirical scaling factors for two-electron integrals. Although his methods for the relativistic corrections and the scaling of the two-electron integrals are difficult to justify from first principles, we found that the numerical values for simpler cases are in good agreement with our relativistic Hartree-Fock calculations, as shown in Table VI-2.

The noticeable discrepancy in wavelengths for the $ns \rightarrow np_{1/2}$ transitions is likely to have come from the approximations used in reference 5 for the spin-orbit splitting. Comparisons on more complex spectral data are in progress.

⁵ R. D. Cowan, Los Alamos Scientific Laboratory Report, LA-6679-MS (1977).

⁶ R. D. Cowan and D. C. Griffin, *J. Opt. Soc. Am.*, **66** (1976) pp. 1010-1014.

Table VI-1. M^2 for Na-Like Ions

Ion	M_{tot}^2	M_{ion}^2	M_{disc}^2	$M_{\text{ion}}^2/M_{\text{tot}}^2$ (%)
Na	8.21	1.36	6.85	17
Fe^{15+}	0.282	0.115	0.167	41
Mo^{31+}	0.0887	0.0395	0.0492	45

Table VI-2. Comparison of Theoretical Spectroscopic Data^a on Fe, Mo, W and Au Ions.

Transition		Semi-relativistic ^b		Relativistic Hartree-Fock	
		$\lambda(\text{\AA})$	f	$\lambda(\text{\AA})$	f
<u>Li-Like Ions</u>					
Fe ²³⁺	2s→2p _{1/2}	260.	0.0176	251.	0.0179
	→2p _{3/2}	195.	0.047	190.	0.0481
Mo ³⁹⁺	2s→2p _{1/2}	150.	0.0109	139.	0.0112
	→2p _{3/2}	58.7	0.056	57.8	0.0565
W ⁷¹⁺	2s→2p _{1/2}	69.1	0.0068	57.7	0.00695
	→2p _{3/2}	6.99	0.134	7.23	0.129
Au ⁷⁶⁺	2s→2p _{1/2}	62.3	0.0065	50.9	0.00659
	→2p _{3/2}	5.24	0.153	5.46	0.147
<u>Na-Like Ions</u>					
Fe ¹⁵⁺	3s→3p _{1/2}	361.	0.126	359.	0.125
	→3p _{3/2}	337.	0.269	334.	0.272
Mo ³¹⁺	3s→3p _{1/2}	178.	0.073	175.	0.0730
	→3p _{3/2}	129.	0.202	127.	0.206
W ⁶³⁺	3s→3p _{1/2}	80.0	0.041	75.0	0.0409
	→3p _{3/2}	22.6	0.293	23.0	0.289
Au ⁶⁸⁺	3s→3p _{1/2}	72.2	0.039	67.2	0.0383
	→3p _{3/2}	17.3	0.325	17.9	0.317

^a λ = transition wavelength, f = absorption oscillator strength.

^b Reference 5.

3. Spectroscopic Data for Ag-Like Ions

To assist in the identification of experimental data on spectral lines for Ag-like ions, we have carried out a systematic calculation on dipole-allowed transitions between low-lying levels using relativistic Hartree-Fock wavefunctions. As expected, the 5s→5p transition is the strongest one, though, from Pm^{14+} ($Z = 61$) and up, the ground state is 4f rather than 5s. Our data were used in confirming the identification of many lines observed by J. Sugar of the National Bureau of Standards on Ag-like ions.⁷ Theoretical and experimental data on Pr^{12+} are compared in Table VI-3. One can see an excellent agreement (better than 1%) between theoretical and experimental data on wavelengths. The discrepancy between the experimental emission intensity and the theoretical transition probability A is an indication of uneven population of the upper states. Population of these states is sensitive to the operating conditions of the light source as well as on the cascade probabilities from still higher states.

⁷ J. Sugar, to be published.

Table VI-3. Comparison of Theoretical and Experimental Data^a on Pr¹²⁺

Transition	Relativistic Hartree-Fock			Experiment ^b	
	$\lambda(\text{\AA})$	f	A ($10^{10}/\text{s}$)	$\lambda(\text{\AA})$	Relative emission intensity
5s→5p _{1/2}	576.5	0.295	0.593	575.998	30
→5p _{3/2}	463.3	0.742	1.15	461.049	200
5p _{1/2} →5d _{3/2}	316.8	1.211	4.02	313.622	100
5p _{3/2} →5d _{3/2}	366.0	0.112	0.556	---	---
5p _{3/2} →5d _{5/2}	356.1	1.031	3.62	352.797	100
4f _{5/2} →5d _{3/2}	259.7	0.102	1.51	258.906	100
4f _{5/2} →5d _{5/2}	254.6	0.007	0.073	---	---
4f _{7/2} →5d _{5/2}	256.8	0.108	1.46	256.054	100
4f _{5/2} →5g _{7/2}	113.8	0.465	18.0	113.415	30
4f _{7/2} →5g _{7/2}	114.25	0.013	0.669	---	---
4f _{7/2} →5g _{9/2}	114.26	0.459	18.7	113.835	50

^a λ = transition wavelength,
f = absorption oscillator strength, and
A = transition probability.

^b Reference 7

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