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Lawrence Livermore Laboratory

Mechanical Design Criteria for Continuously Operating Neutral Beams

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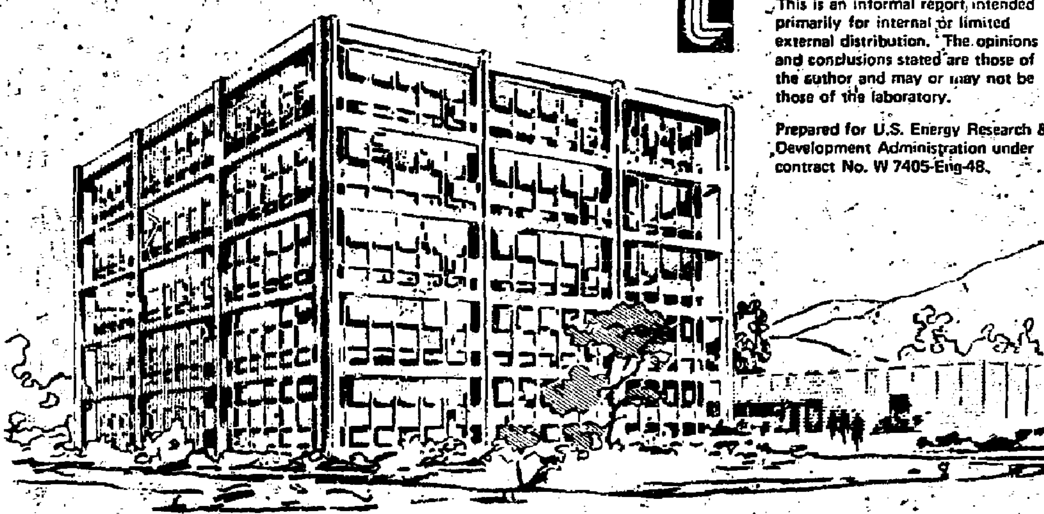
August 26, 1977

*On loan from Westinghouse



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Prepared for U.S. Energy Research & Development Administration under contract No. W 7405-Eng-48.

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NOMENCLATURE

C_A	non-uniform heat flux correction factor
C_p	specific heat of coolant
D_i	inner diameter of grid tubes
e	elementary charge
E	Young's modulas for the grid material
f	friction factor for coolant in grid
F_{10}	fraction of beam neutralized
F_i	fraction of beam causing ionization
h	convective heat transfer coefficient
J_B	beam current density
J_E	current density striking grid
k	thermal conductivity of coolant
k_w	thermal conductivity of grid
K_T	minor pressure loss coefficient
ℓ	grid tube length
L	coolant flow length through the grid
$\dot{m}$	mass flow rate of coolant
M	molecular weight of grid material
n_b	background gas density
N	number of grids connected in series
N_A	Avogadro's number
Nu	Nusselt number
Pp	required fluid pumping power
P_{th}	thermal power deposited on grids
P	pressure of coolant
Pr	Prandtl number
$\dot{q}''$	average heat flux on grid
$\dot{q}_i''$	incident heat flux on grid
r_i	inner radius of grid tubes
r_o	outer radius of grid tubes
R	gas constant for coolant
Re	Reynold's number
S_y	sputter yield in atoms/ion

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t_w tube wall thickness

T grid lifetime

$T_{c,o}$ outlet temperature of coolant

$\bar{T}$ average of coolant inlet and outlet temperatures

v velocity of coolant

X spacing between grids

α coefficient of linear expansion for grid material

β thermal stress correction factor

γ pressure stress correction factor

δ sputtered depth

$\dot{\delta}$ sputter rate

ΔP coolant pressure drop

ΔT_c coolant outlet temperature minus coolant inlet temperature

ΔT_f coolant temperature difference across coolant film

ΔT_w temperature difference across tube wall

μ viscosity of coolant

ν Poisson's ratio for grid material

ρ coolant density

ρ_g grid material density

σ_i ionization cross section

σ_n neutralization cross section

INTRODUCTION

One of the critical needs for a magnetic mirror reactor is that of continuous neutral beams. Present injectors such as those on 2XIIB, deliver pulses a few hundredths of a second long and can operate for only several hours. Commercial reactors require an injector lifetime on the order of one year; thus, many orders of magnitude of improvement is required before an operating reactor could be built.

A schematic of a neutral beam injector is shown in Fig. 1. Neutral gas is injected into the ion source, where a discharge ionizes the gas. The ions are drawn from the source by an extractor grid and then accelerated to full energy by the accel grids.

After acceleration the ions pass through the neutralizer cell. Once through the neutralizer cell, the beam consists of neutrals and ions. The ions traveling with the beam are space charge neutralized by background electrons. The grid which precedes the direct converter is negatively charged and acts to separate the electrons from the rest of the beam. As a result of the beam's uncompensated space charge the remaining ions spread out from the beam to be collected at the direct converter.

Although extensive electrical design of such injectors has been done[1], little work has been done on the mechanical design of continuously operating neutral beam sources. Such a design will limit the allowable current density and beam dimensions.

One obstacle which prevents the continuous operation of neutral beam injectors is the effects which the beam has on the injectors' grids. This paper presents a generalized analysis which will be useful in determining effects of energy and particle fluxes on the long-term performance of the grids.

HEAT TRANSFER

Particle fluxes incident on the grids transfer their kinetic energy to the grids causing the grids to heat. In present injectors, this heat is stored in the grid structure. This method is satisfactory for short pulses, but will not be sufficient for continuous operation. In this analysis of the grids, it is assumed that the heat is continuously being removed from the grid tubes by means of a coolant flowing through the tubes. For added design flexibility it is assumed that the tubes can be hooked up in series producing a total flow length for the fluid which is longer than the length of one tube (see Fig. 2).

Governing Equations

Assuming the heat flux to be constant along the length of the tube, an energy balance on the coolant gives

$$\dot{q}'' \pi D_i L = \dot{m} C_p \Delta T_c .$$

If we connect N tubes in series, each of which has a length ℓ, then the substitution $L = \ell N$ yields

$$\dot{m} = \frac{\pi D_i \ell}{C_p \Delta T_c} \dot{q}'' N . \qquad (1)$$

The Reynold's number and fluid velocity are given by

$$Re = \frac{4\dot{m}}{\pi D_i \mu} = \left(\frac{4\ell}{\mu C_p \Delta T_c} \right) \dot{q}'' N \tag{2}$$

and

$$v = \frac{Re\mu}{D_i \rho} = \left(\frac{4\ell}{D_i \rho \quad C_p \Delta T_c} \right) \dot{q}'' N \quad . \tag{3}$$

In this paper, it will be assumed that the flux striking the grid tubes is either approaching the grid from all directions, or that a flux $\dot{q}''_i$ is impinging on the tube from one side only. The former will be denoted as a uniform flux and the latter as non-uniform flux. If $\dot{q}''_i$ represents the incident flux, then for a non-uniform flux the average and incident fluxes are related by

$$\dot{q}''_i \; D_i L = \dot{q}'' \; \pi D_i L$$

or

$$\dot{q}'' = \frac{\dot{q}''_i}{\pi} \tag{4}$$

for a uniformly heated tube,

$$\dot{q}'' = \dot{q}''_i \quad . \tag{5}$$

For a uniform heat flux the temperature rise in the coolant film is given by

$$\Delta T_f = \frac{\dot{q}''}{h} \quad . \tag{6}$$

However, a hot spot correction factor must be used for the case of a non-uniform flux. This factor, C_A, has been experimentally determined to be equal to 1.3.[2] Equation (6) now becomes

$$\Delta T_f = \frac{\dot{q}_i''}{h} C_A \qquad (7)$$

where $C_A = \begin{cases} 1 \text{ for uniform heat fluxes} \\ 1.3 \text{ for non-uniform heat fluxes} \end{cases}$

and

$$\dot{q}_i'' = \begin{cases} \dot{q}'' \text{ for uniform heat fluxes} \\ \pi\dot{q}'' \text{ for non-uniform heat} \end{cases} .$$

If K_T represents the minor head loss coefficient describing the pressure drop due to the fluid changing direction in the grid structure, then the total minor loss coefficient along the length, L, is equal to K_T (N-1) and pressure drop of the fluid through the tubes is

$$\frac{\Delta P}{P} = \frac{\rho v^2}{2P} \left(4f \frac{L}{D} + K_T (N-1)\right) \qquad (8)$$

$$= \frac{8}{P\rho} \left(\frac{\ell}{D_i}\right)^2 \left(\frac{\dot{q}'' N}{C_p \Delta T_c}\right)^2 \left[4f \frac{\ell}{D_i} N + K_T (N-1)\right]$$

where $f = \begin{cases} 16/Re \text{ for laminar flow} \\ .079\ Re^{-.25} \text{ for turbulent flow} \end{cases} .$

The ratio of pumping power to recoverable thermal power is

$$\frac{P_p}{P_{th}} = \frac{\Delta P\ \dot{m}/\rho}{\dot{q}''\ \pi D_i (\ell N)} .$$

Using equation (1) this reduces to

$$\frac{P_p}{P_{th}} = \frac{\Delta P}{\rho C_p \Delta T_c} . \qquad (9)$$

For turbulent flow, Re > 3000. From equation (2) this requirement becomes

$$\dot{q}'' N > \frac{750\ \mu C_p \Delta T_c}{\ell} . \qquad (10)$$

From equations (7) - (10) we can now derive working equations for various coolants.

Working Equations

Three types of coolants will be examined here, namely; helium, sodium and oil. In addition to the working equations below, equation (9) can be used to find the pump to thermal power ratio.

Helium Cooling

Turbulent flow:

For He, Pr = .7 over a wide temperature range. The correct Nusselt number correlation for turbulent flow is given by:

$$Nu = \frac{hD}{k} = .023\ Re^{.8}\ Pr^{.4} \qquad \text{for } .5 < Pr < 120 \qquad (11)$$

combining (2), (7), and (11)

$$\Delta T_f = 14.34 \frac{\dot{q}_i''\ D_i}{\left(\frac{\dot{q}''\ \ell N}{\Delta T_c}\right)^{.8} (k)^{.2}}\ Pr^{.4}\ C_A \quad . \qquad (12)$$

The behavior of helium can be closely approximated by the ideal gas law, $\rho = P/RT$. When this is substituted into (8), the pressure drop is given by:

$$\frac{\Delta P}{P} = \frac{8\ R\overline{T}}{P^2}\left(\frac{\ell}{D_i}\right)^2 \left(\frac{\dot{q}''\ N}{C_p \Delta T_c}\right)^2 \left[.316\left(\frac{\mu C_p \Delta T_c}{4\ell \dot{q}''\ N}\right)^{.25}\left(\frac{\ell}{D_i}\right) N + K_T\ (N-1)\right] . \qquad (13)$$

Laminar flow:

From (7), with Nu = 4.36 for laminar flow,

$$\Delta T_f = .229\ \frac{\dot{q}_i''\ D_i}{k}\ C_A \quad . \qquad (14)$$

From (8), with $\rho = P/RT$

$$\frac{\Delta P}{P} = \frac{8\ R\overline{T}}{P^2}\left(\frac{\ell}{D_i}\right)^2 \left(\frac{\dot{q}''\ N}{C_p \Delta T_c}\right)^2 \left[16\ \frac{\mu C_p \Delta T_c}{D_i\ \dot{q}''} + K_T\ (N-1)\right] \quad . \qquad (15)$$

Oil Cooling

It may be desirable to use oil to remove the heat from the grids because oil is an electrical insulator. This coolant is restricted to operation at low temperature.

Turbulent flow:

For silicon fluids the Prandtl number is usually greater than one, therefore, equation (11) may be utilized as the proper Nu correlation for turbulent flow. Equations (2) and (7) combined give

$$\Delta T_f = 14.34 \frac{\dot{q}_i'' D_i}{\left(\frac{\dot{\bar{q}}'' \ell N}{\Delta T_c}\right)^{.8} (k)^{.2}} Pr^{.4} C_A \qquad (16)$$

and from (8),

$$\frac{\Delta P}{P} = \frac{8}{P\rho}\left(\frac{\ell}{D_i}\right)^2\left(\frac{\dot{\bar{q}}'' N}{C_p \Delta T_c}\right)^2\left[.316\left(\frac{\mu C_p \Delta T_c}{4\ell \dot{\bar{q}}'' N}\right)^{.25}\left(\frac{\ell}{D_i}\right) N + K_T (N-1)\right]. \qquad (17)$$

Laminar flow:

For laminar flow, Nu = 4.36. Substituting this into (7) gives

$$\Delta T_f = .229 \frac{\dot{q}_i'' D_i}{k} C_A \ . \qquad (18)$$

From equation (8), the pressure drop is

$$\frac{\Delta P}{P} = \frac{8}{P\rho}\left(\frac{\ell}{D_i}\right)^2\left(\frac{\dot{\bar{q}}'' N}{C_p \Delta T_c}\right)^2\left[16 \frac{\mu C_p \Delta T_c}{D_i \dot{\bar{q}}''} + K_T (N-1)\right] . \qquad (19)$$

Sodium Cooling

Although sodium cooling would introduce many complications in the design and operation of an injector, under extremely high heat fluxes this may be the only way to cool the grids.

Turbulent flow:

For low Prandtl numbers at constant heat flux,

$$Nu = 7.0 + .025 (Re\ Pr)^{.8} \qquad \text{for } Pr < .5. \qquad (20)$$

by combining this with (7) and (2), the film temperature rise is

$$\Delta T_f = \frac{\dot{q}_i'' D}{\left(k\left(7.0 + .025\left(\frac{4\dot{q}'' \ell N}{k\ \Delta T_c}\right)^{.8}\right)\right)} C_A \quad . \tag{21}$$

The pressure drop, as in the case of oil cooling is given by

$$\frac{\Delta P}{P} = \frac{8}{P\rho}\left(\frac{\ell}{D_i}\right)^2\left(\frac{\dot{q}'' N}{C_p \Delta T_c}\right)^2\left[.316\left(\frac{\mu C_p \Delta T_c}{4\ell\dot{q}'' N}\right)^{.25}\left(\frac{\ell}{D_i}\right) N + K_T\ (N-1)\right] . \tag{22}$$

Laminar flow:

The equations for laminar flow are the same as for oil, namely;

$$\Delta T_f = .229\ \frac{\dot{q}_i'' D_i}{k}\ C_A \tag{23}$$

and

$$\frac{\Delta P}{P} = \frac{8}{P\rho}\left(\frac{\ell}{D_i}\right)^2\left(\frac{\dot{q}'' N}{C_p \Delta T_c}\right)^2\left[16\ \frac{\mu C_p \Delta T_c}{D_i\ \dot{q}''} + K_T\ (N-1)\right] . \tag{24}$$

SPUTTERING

Since the grids may be subjected to high particle fluxes, sputtering of the grids will be of some concern. The sputter yield, S_y, in atoms ejected per incident ion is shown in Fig. 3 for some grid materials. As can be seen from this graph the maximum sputter yield for D^+ on these grid materials is in the 2 to 30 keV range, which may be the probable energy range for many ions in an injector.

The sputter rate is given by

$$\dot{\delta} = \frac{M}{M_A \rho_g e} S_y J_E \quad , \tag{25}$$

while the lifetime of the grid is

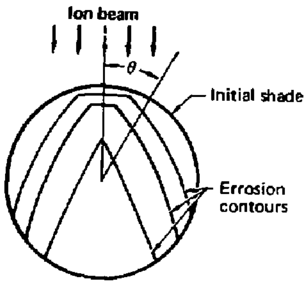
$$\tau = \frac{\delta}{\dot{\delta}} = \frac{\delta N_A \rho_g e}{M\ S_y J_E} \quad . \tag{26}$$

Where J_E is the current density at the grid. The molecular weight, M, and density, ρ_g, are those of the grid material. Values for $\frac{M}{N_A \rho_g e}$ are tabulated in Table 2.

If the flux is uniformly distributed about the grid, the sputtering will also occur at a uniform rate. But in cases of non-uniform particle flux, the problem becomes more serious due to the following approximate angular dependence:

$$S_y(\theta) = \frac{S_y}{\text{Cos } \theta} \quad \text{(See Diagram below)}$$

A result of this angular dependence is that a glancing blow is much worse than a head-on collision. A cross section of the grid will change shape something like this:[3]



Therefore, for a circular cross section, the maximum sputtering rate will be much greater than the value given by equation (26).

TUBE STRESS

The two sources of stress in the grids are thermal and pressure stresses. For both of these there are exact, complicated solutions and thin walled approximations. In thick walled grid designs the approximate formulae become inaccurate. In order to reduce the complications of dealing with the exact equations, correction factors for thermal and pressure stresses will be employed. The exact equations for the point of maximum stress (at the inner surface) are as follows:

$$\sigma_{pressure} = \frac{2 r_i^2}{r_o^2 - r_i^2} P_i \quad (27)$$

and

$$\sigma_{thermal} = \frac{E\alpha \, \Delta T_w}{2\,(1-\nu)\,\ln \frac{r_o}{r_i}} \left(1 - \frac{2\, r_i^2 \, \ln \frac{r_o}{r_i}}{r_o^2 - r_i^2} \right) . \quad (28)$$

The temperature rise in the wall is given by

$$\Delta T_w = \frac{\dot{q}_i'' \, r_o \, \ln \frac{r_o}{r_i}}{k_w} . \quad (29)$$

So $\sigma_{thermal}$ becomes

$$\sigma_{thermal} = \frac{E\alpha \, \dot{q}_i''}{2\,(1-\nu)\,k_w} \, r_o \left(1 - \frac{2\, r_i^2}{r_o^2 - r_i^2} \, \ln \frac{r_o}{r_i} \right) . \quad (30)$$

The thin walled approximations are:

$$\left(\sigma_{pressure}\right)_{approx} = \frac{P_i \, r_i}{t_w} \quad (31)$$

and

$$\left(\sigma_{thermal}\right)_{approx} = \frac{E\alpha \, \Delta T_w}{2\,(1-\nu)} . \quad (32)$$

For thin walls, the temperature rise in the wall is

$$\Delta T_w = \dot{q}_i'' \, \frac{t_w}{k} \quad (33)$$

giving

$$\left(\sigma_{thermal}\right)_{approx} = \frac{E\alpha t_w \, \dot{q}_i''}{2\,(1-\nu)\,k_w} . \quad (34)$$

Using equations (27)-(34) correction factors can be computed as follows:

$$\beta = \frac{\sigma_{thermal}}{\left(\sigma_{thermal}\right)_{approx}} = \frac{r_o}{t_w}\left(1 - \frac{2\, r_i^2}{r_o^2 - r_i^2}\ \ln \frac{r_o}{r_i}\right) \tag{35}$$

$$= \left(1 + \frac{r_i}{t_w}\right)\left(1 - \frac{2}{\left(1 + \frac{t_w}{r_i}\right)^2 - 1}\ \ln\left(1 + \frac{t_w}{r_o}\right)\right)$$

and

$$\gamma = \frac{\sigma_{pressure}}{\left(\sigma_{pressure}\right)_{approx}} = \frac{2\, r_i\, t_w}{\left(r_o^2 - r_i^2\right)} = \frac{2\ \frac{t_w}{r_i}}{\left(1 + \frac{t_w}{r_i}\right)^2 - 1}\ . \tag{36}$$

Equations (35) and (36) are plotted on Fig. 4 for easy reference. We now have

$$\sigma_{pressure} = \frac{P_i\, r_i}{t_w}\,\gamma \tag{37}$$

and

$$\sigma_{thermal} = \frac{E\alpha\ t_w\ \dot{q}_i''}{2\ (1-\nu)\ k_w}\,\beta \tag{38}$$

giving

$$\sigma_{total} = \sigma_{thermal} + \sigma_{pressure} = \frac{E\alpha\ t_w\ \dot{q}''}{2\ (1-\nu)\ k_w}\,\beta + \frac{P_i\, r_i}{t_w}\,\gamma\ . \tag{39}$$

From Fig. 4 it can be seen that the error in σ_{total} made by assuming $\beta = \gamma = 1$ can be kept below 10% for $\frac{t_w}{r_i} < .15$.

We will assume that the design stress is dominated by creep considerations. Denoting the stress for 1% creep over the grid life by σ_{max}. A maximum allowable heat flux can be obtained by setting σ_{total} equal to 60% of σ_{max}. This gives

$$.6\ \sigma_{max} = \frac{E\alpha}{2\ (1-\nu)\ k_w}\ \dot{q}_i''\ t_w\ \ \beta + \frac{P_i\, r_i}{t_w}\,\gamma \tag{40}$$

upon rearranging,

$$\dot{q}_i''{}_{(max)} = \frac{\left(.6\ \sigma_{max} - \frac{P_i\ r_i}{t_w}\right)\gamma}{\frac{E\alpha\ t_w\ \beta}{2\ (1-\nu)\ k_w}} \quad . \tag{41}$$

The material properties used in these equations should be evaluated at the temperature at the middle of the wall at the tube exit (hottest part). For a thick walled tube, the temperature rise as a function of position in the wall is

$$\Delta T_w(r) = \frac{\Delta T_w}{\ln \frac{r_o}{r_i}} \ln \frac{r}{r_i} \tag{42}$$

where

$$\Delta T_w = \frac{\dot{q}_i''\ r_o\ \ln \frac{r_o}{r_i}}{k_w} \quad , \tag{43}$$

therefore,

$$\Delta T_w(r) = \frac{\dot{q}_i''\ r_o}{k_w} \ln \frac{r}{r_i} \tag{44}$$

at the mid-point of the wall, $r = r_i + \frac{t_w}{2}$

$$\Delta T_w\left(r_i + \frac{t_w}{2}\right) = \frac{\dot{q}_i''\ \left(r_i + t_w\right)}{k_w} \ln \left(1 + \frac{t_w}{2\ r_i}\right) \quad . \tag{45}$$

The total temperature of this point is

$$T_w = T_{c,o} + \Delta T_f + \Delta T_w \left(r_i + \frac{t_w}{2}\right) \quad . \tag{46}$$

The stress for 1% creep in 10^4 hours as a function of temperature for various materials is shown in Fig.

SAMPLE CALCULATION

The techniques presented here were used to analyze a proposed injector which extracts 28 amps of D^+ from a Berkeley type ion source. The beam dimensions are 35 by 8 cm with a grid transparency of 50%. The injector is designed so that all of the grids are out of the beamline. The loading on the grids is caused by interaction of the background gas with the energetic beam particles. These interactions cause the background gas to undergo either ionization or charge exchange. The resulting low energy charged particles are then electrostatically accelerated to the grids. The grids themselves have a circular cross section which is 2 mm in diameter with a pitch of 4 mm. The grid wires are actually tubes with .1 mm thick walls, so we have,

$$\ell = .08 \text{ m} \tag{47}$$
$$t_w = 10^{-4} \text{m}$$
$$r_o = 10^{-3} \text{m}$$
$$r_i = r_o - t_w = 9.0 \times 10^{-4} \text{m}$$
$$D_i = 2\, r_i = 1.8 \times 10^{-3} \text{m} \quad .$$

The maximum heat flux for a grid was calculated to be $3 \times 10^7 W/m^2$. In order to determine the temperature of the wall, the thermal conductivity of the grid material must be known. Property values are listed in Table 1 for some grid materials.

Heat Transfer

In this example helium at 60 atmospheres is assumed to be the coolant. In order to recover the heat at temperatures which would be useful in a thermodynamic cycle, it is assumed that the inlet temperature of the helium is 300° C and the outlet temperature is 500° C, giving a $\Delta T_c = 200°$ C.

Equations (12) - (15) are applicable to this problem. The properties of helium for use in these equations are those of its average temperature in the system, or $\overline{T} = \frac{300 + 500}{2} = 400°$ C. These property values are:

$$\overline{T} = 673° \text{ K} \tag{48}$$
$$P = 6 \text{ MPa}$$
$$k = .237 \text{ W/}_{mK}$$
$$C_p = 5187 \text{ J/}_{kgK}$$
$$R = 2077 \text{ J/}_{kgK}$$
$$u = 3.45 \times 10^{-5} \ \frac{N \cdot s}{m^2}$$
$$Pr = .70$$

A majority of the grids in this case are receiving non-uniform heat fluxes, therefore,

$$\dot{\bar{q}}'' = \dot{q}_i'' \pi \tag{49}$$

$$C_A \doteq 1.3 \quad .$$

Combining equations (12) and (47) - (49) we have for turbulent flow

$$\Delta T_f = 50.69 \frac{(\dot{q}_i'')^{.2}}{N^{.8}} \quad . \tag{50}$$

The wall temperature is given by equation (46)

$$T_w = 500 + 50.69 \frac{(\dot{q}_i'')^{.2}}{N^{.8}} + 5.27 \times 10^{-5} \frac{\dot{q}_i''}{k_w} \quad . \tag{51}$$

The pressure drop is given by a combination of equations (13) and (47) - (49) with a $K_T = 1.0$

$$\frac{\Delta P}{P} = 5.87 \times 10^{-17} \left(\dot{q}_i'' N\right)^2 \left[60.8 \frac{N^{.75}}{\left(\dot{q}_i''\right)^{.25}} + (N-1)\right] \quad . \tag{52}$$

For turbulent flow, equation (10) requires that

$$\dot{q}_i'' N > 1.05 \times 10^6 \frac{W}{m^2} \quad . \tag{53}$$

Equations (51) - (53) are plotted on Fig. 6 for TZM and Fig. 7 for 316 stainless steel.

From equation (14) for laminar flow,

$$\Delta T_f = 2.26 \times 10^{-3} \dot{q}_i'' \tag{54}$$

giving

$$T_w = 500 + 2.26 \times 10^{-3} \dot{q}_i'' + \frac{5.27 \times 10^{-5}}{k_w} \dot{q}_i'' \tag{55}$$

and from equation (15) with $K_T = 1.0$,

$$\frac{\Delta P}{P} = 5.87 \times 10^{-17} \left(\dot{q}_i'' N\right)^2 \left[\frac{9.99 \times 10^5}{\dot{q}_i''} + (N-1)\right] \tag{56}$$

while the limit for the use of these equations is similar to (53),

$$\dot{q}_i'' N < 1.05 \times 10^6 \frac{W}{m^2} . \tag{57}$$

For the heat flux ranges being considered here laminar flow produces wall temperatures which exceed the melting point of even tungsten. Therefore, these equations will not be presented graphically.

Thermal Stress

Using equation (41) we can now compute the maximum allowable heat flux at different wall temperatures. We have

$$(\dot{q}_i'')_{max} = \frac{.6\sigma_{max} \quad -6 \times 10^7}{\frac{E\alpha}{2(1-\nu)k_w} \quad 1 \times 10^{-4}} . \tag{58}$$

Values for σ_{max} are shown in Fig. 4, while values for $\frac{E\alpha}{2(1-\nu)k_w}$ and $\left(\dot{q}_i''\right)_{max}$ for this example are in Table 2.

Equations (55) - (58) give a maximum allowable heat flux, for helium cooling, which is dependent on the grid material. These equations are plotted on Fig. 6 for TZM and Fig. 7 for 316 stainless steel. A comparison of these figures reveals that with a coolant pressure loss of 1% TZM grids can withstand a heat flux of $3 \times 10^6 W/m^2$, provided that 3 tubes are connected in series. For 316 stainless steel grids with 13 tubes in series $2 \times 10^5 W/m^2$ is the allowable maximum heat flux.

When the equations for sodium cooling are applied to this problem, it is found that for a $\Delta P/P$ of 50% and an inlet pressure of one atmosphere, heat fluxes of up to $5.5 \times 10^7 W/m^2$ can be handled with TZM Grids. Stainless steel grids can operate at up to $1.5 \times 10^6 W/m^2$.

Oil cooling is not effective due to the high viscosity of oil and the small tube diameters.

Sputtering From equation (26) the lifetime of the grid is

$$T = \frac{\delta}{S_y J_E} \frac{N_A \rho_g e}{M} .$$

For this proposed injector, the maximum particle current impinging on a grid was calculated to be about $4 \times 10^5 \frac{A}{m^2}$ of 5 keV D^+. The time required for complete erosion of 10^{-4}m of TZM is

$$T = \frac{10^{-4} m}{10^{-3} \frac{atoms}{ion} \cdot 4 \times 10^5 \frac{A}{m^2} \cdot 9.87 \times 10^{-11} \frac{m^3}{coul}} = 2500s,$$

or about 20 minutes. Due to glancing blows the actual time for erosion of the grids is probably one-fourth of this. While this lifetime would be acceptable in present injectors, it would clearly not be for continuous injectors.

RECOMMENDATIONS

It appears that the amount of sputtering which the grids experience sets a maximum value on their lifetime. There are several features of the injector which may be manipulated to prolong grid lifetime. The current density striking a grid can be approximated by [4]

$$J_E = J_B (F_{10} + F_i)$$

where

$$F_{10} + F_i = (\sigma_n + \sigma_i) n_b X .$$

Three variables which lend themselves to manipulation are J_B, n_b and X. If J_B is to be decreased, then more injectors will be necessary to produce the required current. The background gas density, n_b, can be decreased by pumping harder. Unfortunately the small space available and the power consumption of pumps severely limit the amount by which n_b can be decreased. The third variable X, corresponds to the axial spacing of the grids. The addition of more grids which are more closely spaced would, therefore, reduce the incident ion flux on the grids. A fourth method of reducing sputtering would be to change the grid tube shape. It was pointed out that the sputtering yield increases as the incident particle angle of attack increases. A grid which deviates from a circular cross section by presenting a flat face to the incoming particles would accomplish this.

Another method, would be to clad the grids with a material which does not easily sputter. A good candidate would be tungsten, which sputters less than other available materials.

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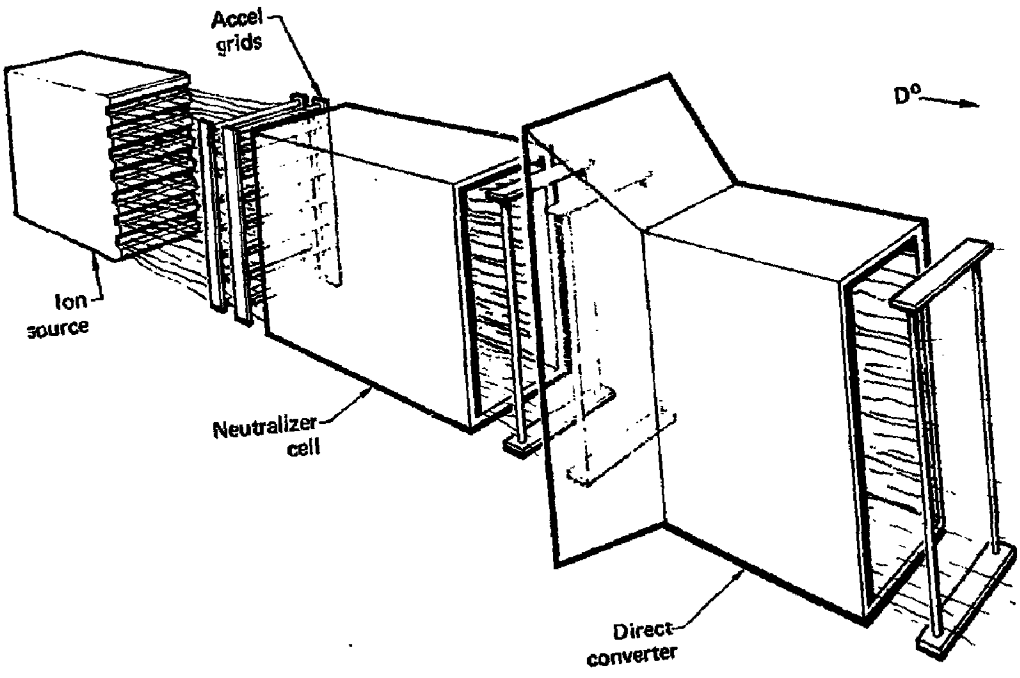
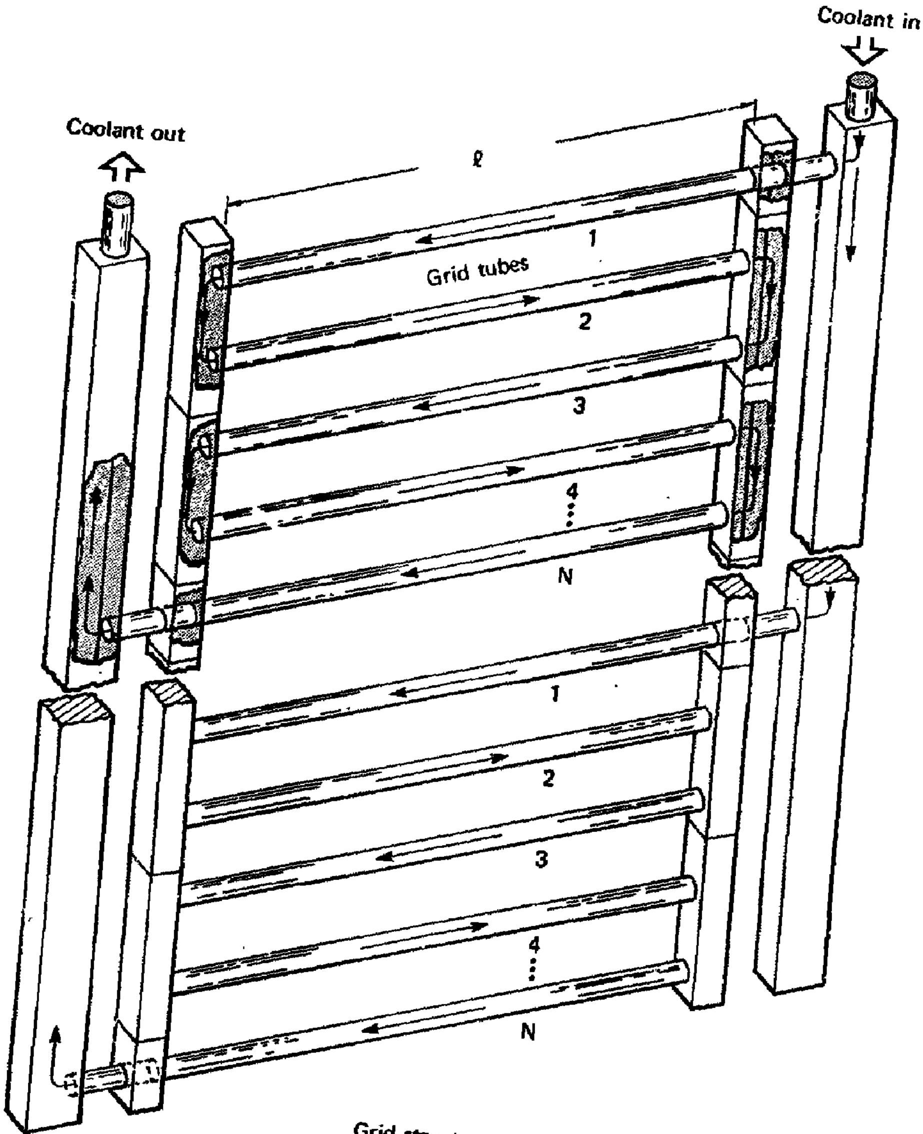


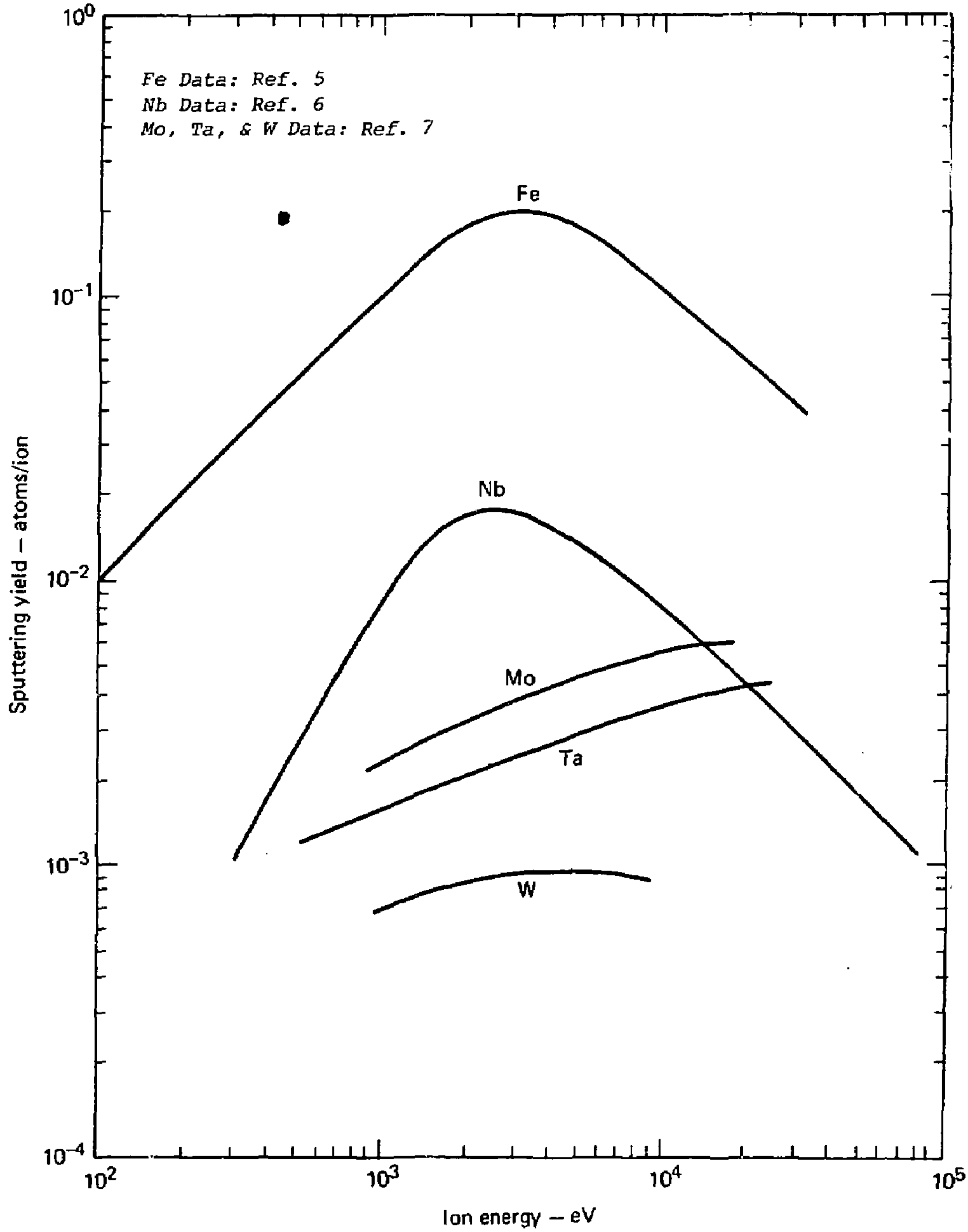
(Not drawn to scale)

Figure 1



Grid structure

Figure 2



Fe Data: Ref. 5
Nb Data: Ref. 6
Mo, Ta, & W Data: Ref. 7
Fe
Nb
Mo
Ta
W
Sputtering yield — atoms/ion
Ion energy — eV
10^0
10^{-1}
10^{-2}
10^{-3}
10^{-4}
10^2
10^3
10^4
10^5

Figure 3

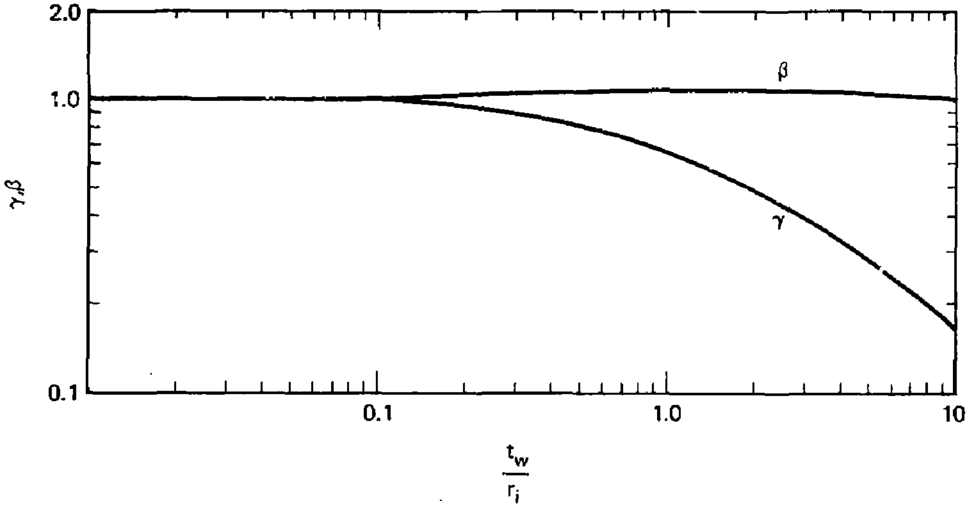
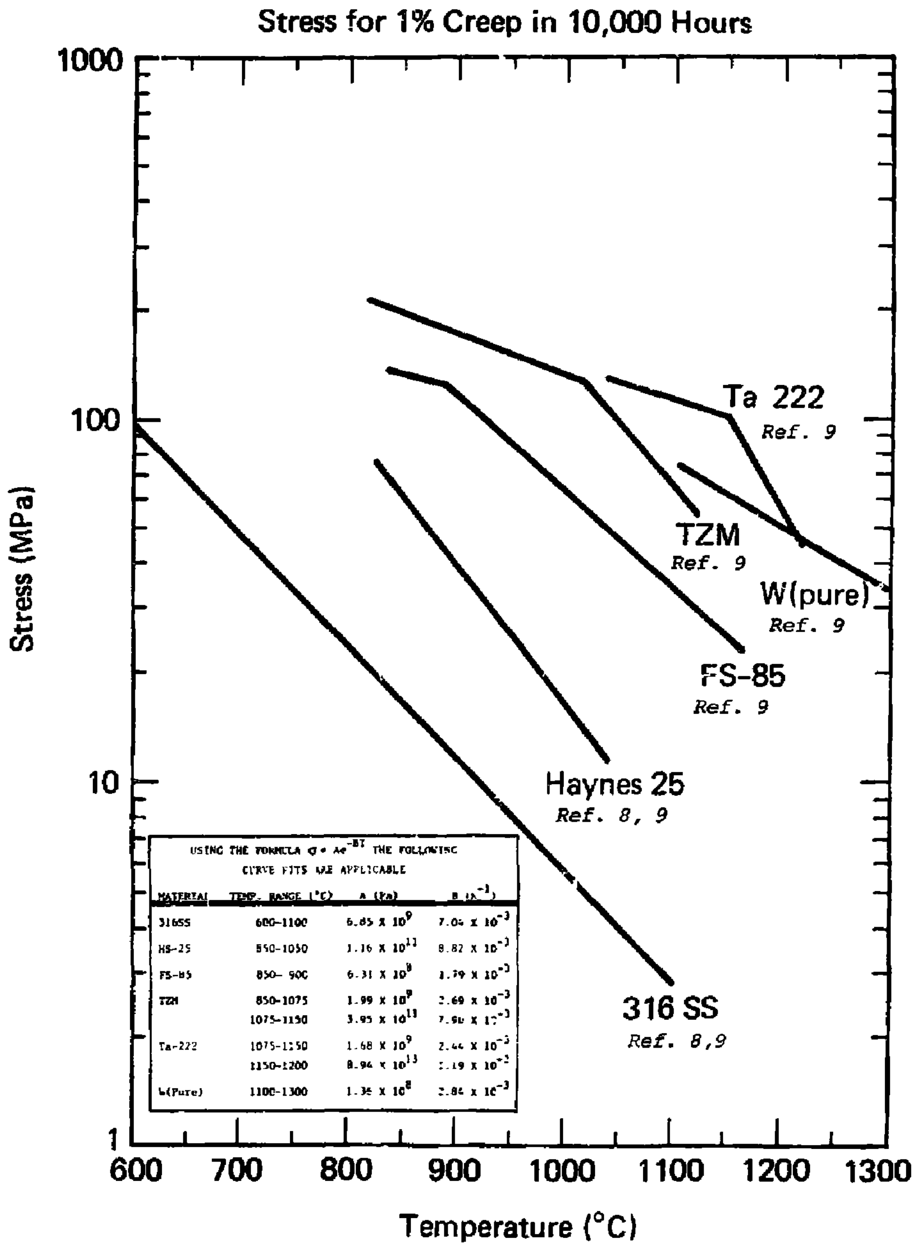


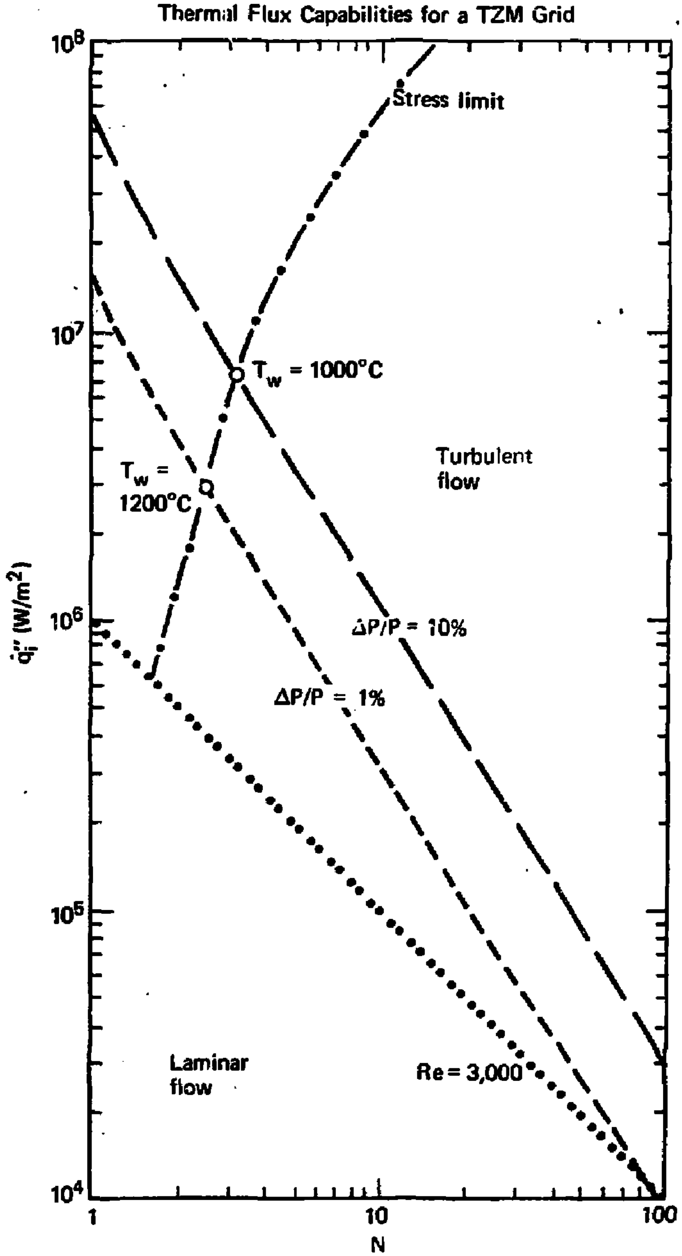
Figure 4



USING THE FORMULA $\sigma = Ae^{-BT}$ THE FOLLOWING CURVE FITS ARE APPLICABLE

MATERIAL	TEMP. RANGE (°C)	A (Pa)	B (K^{-1})
316SS	600-1100	6.85×10^{9}	7.04×10^{-3}
HS-25	550-1050	1.16×10^{11}	8.82×10^{-3}
FS-85	850- 900	6.31×10^{8}	1.79×10^{-3}
TZM	850-1075	1.99×10^{9}	2.69×10^{-3}
	1075-1150	3.95×10^{11}	7.96×10^{-3}
Ta-222	1075-1150	1.68×10^{9}	2.44×10^{-3}
	1150-1200	8.94×10^{13}	2.19×10^{-2}
W(Pure)	1100-1300	1.35×10^{8}	2.84×10^{-3}

Figure 5



Thermal Flux Capabilities for a TZM Grid
Stress limit
T_w = 1000°C
T_w = 1200°C
Turbulent flow
ΔP/P = 10%
ΔP/P = 1%
Laminar flow
Re = 3,000
q̇i'' (W/m²)
N
10^8
10^7
10^6
10^5
10^4
1
10
100

Figure 6

Thermal Flux Capabilities for a 316 Stainless Steel Grid

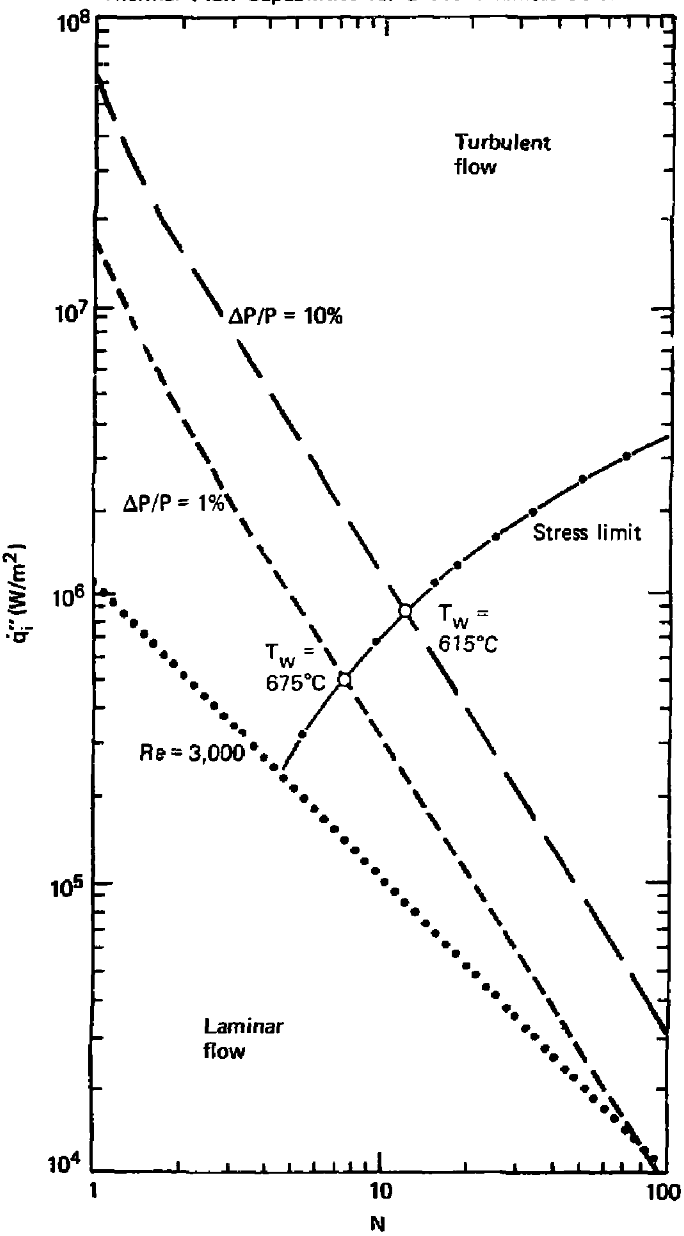


Figure 7

MATERIAL PROPERTIES

TABLE 1

MATERIAL	T (°C)	E (GPa)	ν	$\alpha X 10^6$ (K^{-1})	k ($^W/mk$)
316SS	600	148	.310	18.75	22.4
	700	138	.317	18.95	23.7
	800	127	.325	19.20	25.1
HS-25	600	179	.288 *	15.3	20.4
	800	175		16.4	24.4
	1000	171		17.5	28.5
TZM	850	234	.321 *	5.4	106
	1000	207		7.0	101
	1100	186		7.9	98
	1200	168		9.0	95
Ta-222	900	162	.35 *	16.2	59.2
	1200	156		17.1	63.7
FS-85	850	106	.38 *	9.32	53.1
	1000	101		9.72	55.4
	1150	96.5		11.1	56.9
W(pure)	800	366	.296	4.6	117
	1000	357	.298	4.8	113
	1200	347	.300	4.9	109

*At room temperature

MATERIAL PROPERTIES

TABLE 2

MATERIAL	$T(^oC)$	$\frac{M}{N_A \rho_g e}$ (m^3/coul)	$\frac{E\alpha}{2(1-\nu)k_w}$ X 10^{-4}	$(\dot{q}_i'')_{Max}$ FOR SAMPLE CALCULATION
316SS	600	7.36 X 10^{-11}	8.98	*
	700		8.08	*
	800		7.20	*
HS-25	600	6.86 X 10^{-11}	9.41	*
	800		8.26	*
	1000		7.37	*
TZM	850	9.75 X 10^{-11}	.878	6.83 X 10^7
	1000		1.06	1.13 X 10^7
	1100		1.10	*
	1200		1.15	*
Ta-222	900	1.13 X 10^{-10}	3.41	1.58 X 10^7
	1200		3.41	*
FS-85	850	1.15 X 10^{-10}	1.50	7.2 X 10^6
	1000		1.43	*
	1150		1.52	*
W(pure)	800	9.87 X 10^{-11}	.938	8 X 10^7
	1000		1.07	1.12 X 10^7
	1100		1.09	*
	1200		1.11	*

* At these temperatures the pressure stresses alone exceed 60% of σ_{Max} for the sample calculation. Hence it would not be reasonable to operate these materials at the temperatures indicated for any heat flux.