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by

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DEUTERIUM FLOW LOOP FOR
A SUPERSONIC GAS-TARGET INTENSE NEUTRON SOURCE

by

G. Edward Cort

ABSTRACT

One of the milestones to be passed in the National Controlled Thermonuclear Research (CTR) program is a thorough knowledge of the effects of high-level neutron irradiation on materials. The early needs for a high-fluence 14-MeV Intense Neutron Source (INS) to carry out such relevant studies can best be met by the supersonic gas-target approach. The INS will consist basically of two intersecting beams, one of 1.5 A of 270-keV tritium ions and the other a supersonic jet of deuterium molecules. The collisional intersection of these two beams will be the source of 14-MeV neutrons from the resultant D-T reaction.

Parametric calculations of inviscid one-dimensional gas dynamics and heat transfer in the deuterium flow loop (consisting of inlet plenum, target area, supersonic and subsonic diffuser sections, heat exchanger, and compressor) are described in this report. The objective was to size components and estimate operating conditions for the loop. Three parameters are relatively fixed for the INS design being considered: 1) the kinetic energy of the tritium ion beam is 300 kW; 2) the density of the deuterium gas in the target area (where the beams intersect) should be 2×10^{19} molecules/cm³ for adequate stopping power; 3) the diameter of the target area should be small (<1.5 cm) to maximize the neutron flux. With these limitations, and to satisfy the nonchoking condition at the exit of the target, the entering deuterium must be at high pressures (44 MPa) and stagnation temperatures (858 K) with a Mach number of about 6. The deuterium mass flow is 0.08 kg/s and the stagnation temperature leaving the target area is 1375 K. The cycle compression ratio is about 40 and the ideal compressor work is 430 kW.

Parametric curves are given which include the sizes of components and the gas dynamic conditions throughout the deuterium loop.

The conceptual design appears to be feasible, but experimental studies are recommended to investigate effects of boundary layers in conjunction with the supersonic flow and heat addition.

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I. INTRODUCTION

A. Objectives

The Los Alamos Scientific Laboratory (LASL) has proposed¹ to establish a 14-MeV Intense Neutron Source (INS) for research into the effects of high-level neutron irradiation on materials. The neutrons are to be used primarily to study radiation damage phenomena similar to those anticipated in Controlled Thermonuclear Research (CTR) reactor structural

materials. The early needs for such a high fluence 14-MeV neutron source to carry out these relevant studies can probably best be met by the supersonic gas-jet target approach. The INS will consist basically of two intersecting beams, one of 1.5 A of 270-keV tritium ions and the other a supersonic jet of deuterium molecules. The collisional intersection (or target) of these two beams will be the source of 14-MeV neutrons from the resultant D-T reaction.

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The innovative feature of the INS facility is development of the supersonic deuterium-gas target, operating in a vacuum. Heat depositions can thereby be made several orders of magnitude higher than in comparable size solid targets, with higher neutron-source intensities the result. However, the very problem of heat deposition in the deuterium jet introduces several new problems to the fluid dynamical studies in the interaction region, where the tritium-ion beam is in collision with the deuterium jet. The deuterium jet becomes tritium-contaminated at the target area. To satisfy radiological safety standards, the contaminated deuterium must be contained and the loop closed through a pressure recovery system. The primary elements in the deuterium loop (Fig. 1) are the diffuser, heat exchanger, tritium separator, compressors, and the jet nozzle. This report describes results of parametric calculations to size components and establish flow conditions in the deuterium loop. The calculations are preliminary in nature because they are based on inviscid, one-dimensional, ideal gas assumptions. Detailed numerical calculations and experiments will be necessary to refine these estimates. The results are useful at this early stage in the program to explore feasibility, make cost estimates and programmatic plans, and direct the emphasis for the more detailed studies.

B. Requirements

Referring to Fig. 1, the energy added in the target area, ΔQ , is the kinetic energy of the tritium ion beam. The D-T reaction energy is negligible by comparison. It is desirable to minimize both the pressure losses in the system and the size of the target area. A small target area yields a higher neutron flux for a fixed beam strength, but it also requires high system temperatures and pressures.

Fig. 1. Schematic of deuterium flow loop.

This will place severe requirements on structural materials in the flow loop. The deuterium density in the target area should be 2×10^{19} molecules/cm³ (0.13 kg/m³) for adequate stopping power of the tritium ion beam¹. The tritium beam power, $\Delta Q = 300$ kW, was held fixed in these calculations while all other system variables were changed parametrically.

II. RESULTS

The discussion is divided into three parts, dealing with the deuterium expansion nozzle and interaction target area first, then the flow loop, and finally details of the heat exchanger itself.

A. Nozzle and Target Area

1. Introduction

The deuterium flow enters an expansion nozzle with a certain stagnation pressure (P_{01}) and stagnation temperature (T_{01}). The flow is expanded to supersonic velocity and immediately enters the target area where it is intersected by the tritium-ion beam (Fig. 2).

Leakage from the deuterium jet into the ion-beam tube will determine the vacuum pumping requirements. Experiments and two-dimensional flow calculations will be necessary to determine the deuterium leakage, but it will increase with increasing ratio of energy deposition to entering gas kinetic energy, $f = \frac{\Delta Q}{1/2 \dot{m} V^2}$, and decrease with increasing entering Mach number, M_1 , where $\dot{m}$ = deuterium mass flow and V = deuterium velocity.

2. Maximum Energy Addition

When the value of ΔQ , M_1 , and target gas density ρ_1 are fixed, the product of f and the target

Fig. 2. Schematic of nozzle and target area.

flow area A_1 is uniquely related to the entering stagnation temperature T_{01} . This follows from manipulation of the isentropic flow equation relating static and stagnation temperatures, and the definitions of f and M_1 . The entering stagnation temperature must satisfy:

$$T_{01} = \frac{(1 + \frac{\gamma-1}{2} M_1^2)}{\gamma R M_1^2} \left(\frac{2 M_1^2}{\gamma M_1^2} \right)^{2/\gamma} \quad (1)$$

where

γ = specific heat ratio = 1.4;

R = gas constant = 2876 J/(kg·K).

When A_1 and f are both reduced (desirable), T_{01} increases. The higher temperature places demands on materials of construction which must contain the high pressure gas; however, it appears that a satisfactory design can be achieved as will be shown. Figure 3 shows the relationship between T_{01} and $f A_1$ for constant M_1 . High density and high M_1

Fig. 3. Product of flow area and energy deposition fraction versus inlet stagnation temperature.

cause T_{01} and $f A_1$ to be reduced; however, there are diminishing returns with respect to M_1 .

The choking phenomenon in a constant area duct with heat addition results in another limitation on f . The derivation starts with the energy equation,

$$\dot{Q} = \dot{M} C_p (T_{02} - T_{01}) \quad (2)$$

where

C_p = specific heat = 7245 J/(kg·K), and

T_{02} = target exit stagnation temperature.

After rearranging and using the definitions of f , M_1 , and the isentropic flow equation relating static and stagnation temperatures,

$$\frac{T_{02}}{T_{01}} = 1 + \frac{f}{2} \frac{(\gamma-1) M_1^2}{(1 + \frac{\gamma-1}{2} M_1^2)} \quad (3)$$

Choking occurs when $M_1 = 1$ and $T_{02} = T^*$. For fixed values of M_1 , T^*/T_{01} can be found from the Rayleigh line and Eq. (3) solved for f .

A more elegant treatment is found in Ref. 2 where the simultaneous addition of heat, mass, and momentum to a stream tube for one-dimensional flow results in the following limiting condition:

$$\frac{(1+a) (1+b)^2}{(1+b)^2} = \frac{(1 + \gamma M_1^2)}{2 (1 + \frac{\gamma-1}{2} M_1^2)} \quad (4)$$

where a , b , and c are nondimensional additions of mass, momentum, and energy, respectively, defined in the nomenclature. Setting $a = b = 0$ and making proper substitutions, Eqs. (3) and (4) can be shown to be equivalent, which is comforting but not very useful. What is useful is that by proper choice of momentum addition, choking can be avoided. For $M_1 = 1$, the momentum addition produces an acceleration (expansion of cross section), which, if it occurs sufficiently downstream of the high neutron flux zone, can take place without increasing A_1 . Figure 4 shows f versus area ratio for various entering Mach numbers.

3. Relaxation Time Scale

The above neglects the fact that the heat addition and pressure equalization require time. The parameter of importance is the ratio of the time-scale of the relaxation, t_R , to that of the flow,

Fig. 4. Maximum energy deposition ratio in frictionless duct.

$t_f = l/V_1$, where l is the length of interest (say 0.01 m). This is the Damköhler number D , defined by

$$D = \frac{t_f}{t_R} = \frac{l}{V_1 t_R}.$$

There are three relaxation times of interest; the first is the time to deposit the ion beam's kinetic energy in the deuterium gas. If the tritium ions are stopped in a series of electron-ion collisions, in which the ion energy is considerably greater than the average electron energy, the time constant³ for the ion's energy to be reduced by a factor of $\frac{1}{e}$ is $\tau = 1.1 \times 10^{12} T_e^{3/2}/N_e$ where T_e is the electron energy in keV and N_e is electron density. With an ionized electron density of 2×10^{17} per cm^3 ($\frac{1}{100}$ the deuterium molecular density), and $T_e = 0.036$ keV, $\tau = 4 \times 10^{-8}$ s. Within 31 or 0.1 μs then, the ions lose 95% of their initial kinetic energy. The deuterium gas velocity is typically 3 km/s, so $t_f = 3 \mu\text{s}$ and $D \approx 30$. Therefore, the heat deposition takes a short time and the flow in the target area may choke.

The second relaxation time of interest is for the D-T reaction to take place. Again using Ref. 3, this is estimated to be about 10 μs and $D \approx 0.3$. Since the D-T reaction is about two orders of magnitude slower than the ion-electron collision rate,

it will be difficult to enlarge the duct to accommodate potential choking. More precise calculation of the above rates will be needed before final conclusions can be made, however.

The third relaxation time of interest is the time for radial pressure equalization after the heat is deposited (concentrated at the centerline). Because the heat deposition is complex both radially and axially and because of oblique shock waves in the target area, only a rough order of magnitude estimate can be made. Assume all of the energy is deposited uniformly in a 5-mm-radius cylinder projected from the beam tube, and none is deposited in the 2-mm-thick annulus surrounding it (Fig. 2). Assume the heat is deposited instantaneously at constant volume (constant density) in this zone causing a radial discontinuity in pressure and temperature. Instead of looking at the Damköhler parameter, we equate the time for the gas to travel axially a distance, l of 10 mm with the time for the shock wave to travel radially a distance of 2 mm. This results in a desired velocity ratio $U/V_1 \leq 0.2$, where U represents the radial velocity either of the shock wave or the following contact discontinuity. Using the normal shock tables as applied to a shock tube,⁴ Fig. 5 shows the velocity ratios versus

Fig. 5. Radial velocity of idealized contact discontinuity caused by nonuniform heating.

entering Mach numbers. The radial velocities are the maximum values, before the pressures have begun to equalize, so that time-integrated average velocity ratios will be less. Still, the pressures will equalize relatively quickly.

The relaxation phenomena have been ignored in the work which follows and the duct diameter is held constant. To avoid choking, the parametric study limits the target exit Mach number, $M_2 > 1$, and the ratio of energy deposition to entering gas kinetic energy, $f < 1$.

4. Static Pressure Recovery in Target Area

The static pressure recovery influences design and performance of downstream components, but is hard to predict because of complex shock waves in the target area. An upper limit might result from a normal shock and a lower limit from following the Rayleigh line (no shocks). Perhaps the most realistic one-dimensional estimate would result from the Hugoniot equation⁵, derived from the conservation laws and valid for all types of gas dynamic discontinuity.

$$h_2 - h_1 = 1/2 (P_2 - P_1) \left(\frac{1}{\rho_1} + \frac{1}{\rho_2} \right),$$

where h_2 , h_1 are exit and entering enthalpies, P_2 , P_1 static pressures, and ρ_1 , ρ_2 densities. If it is assumed the change in gas density is small, $\rho_2 \approx \rho_1$, rearrangement and substitution of isentropic relations gives:

$$P_2/P_1 = 1 + \frac{\gamma}{\gamma-1} \left(1 + \frac{\gamma-1}{2} M_1^2 \right) \left(\frac{T_{02}}{T_{01}} - 1 \right).$$

Figure 6 compares the static pressure recovery based on the three methods.

B. Deuterium Flow Loop

1. Method and Assumptions

Flow conditions around the loop sketched in Fig. 1 were calculated by a small computer program for fixed values of ΔQ , M_1 , M_2 , and ρ_1 . Inviscid, one-dimensional, ideal gas assumptions were used. The following additional assumptions were made.

- Static pressure recovery in the target area was based on either the Rayleigh line or the Hugoniot equation for constant density. Figure 7 illustrates the effect of this assumption for constant inlet and exit Mach number.

Fig. 6. Static pressure recovery in target area.

- Static pressure recovery in the supersonic diffuser section is based on a normal shock.⁶ A second throat diffuser could be more efficient, but is not justified for low inlet Mach numbers.
- The subsonic diffuser section is sized with a 3° half-angle to achieve an exit Mach number of 0.1. Its assumed pressure efficiency is 0.8.
- The heat exchanger is a single tube, concentric, counterflow-type with nonboiling water in the outer annulus. The gas exit temperature and water inlet and outlet temperatures remain fixed at 400 K, 290 K, and 336 K, respectively. The thickness of the tube separating the two flows is 10% of the inner diameter and its thermal conductivity is 25 W/(m·K) stainless steel).
- D_2 pressure loss in the heat exchanger is based on turbulent flow, with the Reynolds

Fig. 7. Static pressure recovery in target area.

analogy between heat and momentum transfer remaining valid. This is a good assumption for gases.

- The ideal pumping power is based on isentropic compression from an inlet temperature of 400 K and the heat exchanger exit pressure (no pressure losses for piping, valves, or fittings).
- The stagnation inlet temperature to the nozzle is equal to 75% of the adiabatic compressor exit temperature to allow for unavoidable cooling. This results in the desired high inlet temperature (Fig. 3) without the need for auxiliary heating.
- The nozzle stagnation pressure is based on the specified target inlet Mach number, density, and the temperature above.

2. Results

Figure 8 shows the relationship between the inlet and exit Mach numbers and the energy deposition ratio. The energy deposition ratio is maximized when M_1 is high and M_2 approaches 1. In the parametric calculations, M_1 ranged from 3 to 7 and M_2 from 1.1 to 1.9. Figure 9 shows the cycle compression ratio to be strongly dependent on M_1 and not sensitive to M_2 for the range studied. Target inlet stagnation temperature (based on 75% of the isentropic pump outlet temperature) shows similar sensitivity to M_1 and M_2 as expected (Fig. 10). The ideal pump work is less sensitive to M_1 because of the compensating effect of the mass flow rate's decrease with increasing M_1 (Fig. 11). Pump work increases with M_2 because mass flow increases and also because of the increased inefficiency of supersonic diffusion with higher Mach numbers (Fig. 12). Figure 13 shows target stagnation pressure increases rapidly beyond $M_1 = 6$. In Figure 14, the energy deposition ratio, f , is insensitive beyond $M_1 = 6$, but is very dependent on M_2 . Target area decreases with increasing M_1 and with decreasing M_2 . To hold the target area less than 2 cm² without a large increase in pressure, requires $M_1 = 6$ and a target inlet pressure near 45 MPa (6500 psi). The choice of M_2 involves a tradeoff between f and target area which must be made on the basis of two-dimensional calculations and experiments. For $M_1 = 6$ and

Fig. 8. Target area exit Mach number versus energy deposition ratio.

Fig. 9. Cycle compression ratio.

Fig. 10. Target inlet stagnation temperature.

Fig. 11. Ideal pump work.

Fig. 12. Compression ratios for various conditions of diffusion.

Fig. 13. Target stagnation pressure.

Fig. 14. Target area and energy deposition ratio.

$M_2 = 1.7$, for example, $f = 0.69$ and $A_1 = 1.86 \text{ cm}^2$. Table I summarizes a set of reference conditions for the loop. For the example conditions, the D_2 stagnation temperature entering the target area is 850K (1070°F) (Fig. 10) and the ideal pump work is 430 kW. Target exit stagnation temperature is about 1375K (2000°F) (Fig. 15); water flow in the heat exchanger is about 3.75 kg/s (Fig. 16); and the D_2 flow rate is 0.08 kg/s (Fig. 17).

For the figures just presented, D_2 gas density in the target area was held fixed at 0.13 kg/m^3 (2×10^{19} molecules per cm^3). Figures 18 and 19 show how system variables are affected by changes in gas density for constant M_2 . With fixed target

Fig. 15. Stagnation temperature at target.

Fig. 16. Water flow.

Fig. 17. Deuterium flow.

TABLE I
REFERENCE CONDITIONS FOR D₂ LOOP^a

M ₁	Mach number entering target	6	V ₄	Velocity exiting subsonic diffuser	200 m/s
M ₂	Mach number leaving target	1.7	d ₄	Diameter of subsonic diffuser exit and heat exchanger i.d.	0.0355 m
ρ ₁	Gas density entering target	0.13 kg/m ³ (2x10 ¹⁹ molecules/cm ³)	A ₄	Flow area of subsonic diffuser exit and heat exchanger	0.001 m ²
A ₁	Target flow area	1.86x10 ⁻⁴ m ²	L ₄₋₅	Length of heat exchanger	12.5 m
ΔQ	Ion beam energy	300 kW	LMTD	Heat exchanger log-mean temperature difference	366.2 K
V ₁	D ₂ velocity entering target	3300 m/s	m _w	Water flow rate	3.75 kg/s
T ₀₁	Stagnation temperature entering target	858 K	T _{in} /T _{out}	Water inlet and outlet temperature	290/330 K
T ₁	Static temperature entering target	104.6 K	Re _w	Water Reynolds number	6.1x10 ⁴
P ₀	Stagnation pressure at target	44.4 MPa (6440. psia)	h _w	Average water side heat transfer coefficient	5815 W/(m ² ·K)
P ₁	Static pressure at target	28.1 kPa	A _w	Water side flow area	3.05x10 ⁻³ m ²
m	D ₂ mass flow	0.08 kg/s	V _w	Water velocity	1.23 m/s
f	ΔQ / (½ m V ₁ ²) energy deposition ratio	0.69	d _i	o.d. of inner concentric tube in heat exchanger (water passage i.d.)	0.0426 m
T ₀₂	Stagnation temperature leaving target	1375 K	d _o	i.d. of outer concentric tube in heat exchanger (water passage o.d.)	0.0755 m
T ₂	Static temperature leaving target	871 K	T̄	Average heat exchanger wall temperature	400 K
P ₂	Static pressure leaving target	286.7 kPa	T _{max}	Maximum heat exchanger wall temperature	520 K
V ₂	Target exit velocity	2701 m/s	U	Overall heat transfer coefficient for heat exchanger	1026 W/(m ² ·K)
ρ ₂	Target exit density	0.16 kg/m ³	P _w	Suggested water pressure (P _{sat} at T _{max})	3.79 MPa (550 psia)
P ₂ /P ₁	Static pressure ratio at target	10.2	T ₅	Deuterium exit temperature from heat exchanger (specified for design)	400 K
d ₂	Diameter of supersonic diffuser (and target duct)	1.54x10 ⁻² m	Re ₄₋₅	Average deuterium Reynolds number in heat exchanger	1.11x10 ⁵
L ₂₋₃	Length of supersonic diffuser section (assuming 100 kW of cooling for control of wall temperature)	~1.2 m	h ₄₋₅	Average deuterium heat transfer coefficient	1515 W/(m ² ·K)
Re ₃	Reynolds number in supersonic diffuser	2.77x10 ⁵	P ₅	Exit static pressure from heat exchanger	1.125 MPa (163 psia)
M ₃	Mach number exiting supersonic diffuser	0.6	P ₄₋₅	Heat exchanger pressure drop	28 kPa (4.1 psi)
P ₃	Static pressure exiting supersonic diffuser	0.91 MPa	P	Ideal isentropic compressor work	431 kW
T ₀₃	Stagnation temperature exiting supersonic diffuser	1202 K	P ₀₁ /P ₅	Cycle compression ratio	35.5
L ₃₋₄	Length of subsonic diffuser (assuming 3° half-angle)	0.19 m	T ₄	Adiabatic compressor exit temperature	1144 K
M ₄	Mach number exiting subsonic diffuser	0.1			
P ₄	Static pressure exiting subsonic diffuser	1.16 MPa			

^a Conditions at Station 2 (target exit) are based on the Rayleigh line, with no shocks in the target area. This gives conservatively low pressure recovery.

Fig. 18. Target stagnation pressure versus deuterium density, $M_2 = 1.1$.

Fig. 19. Target area versus deuterium density.

inlet and exit Mach number, ΔQ fixed, and target gas density varying; the ideal pump work, energy deposition fraction, target inlet and exit stagnation temperature, and D_2 flow are all constant. The system pressure levels and target flow area are the only significant parameters that vary.

C. Heat Exchanger

1. Diffuser Section

One of the purposes of all the preceding was to arrive at reasonable and consistent parameters (D_2

flow, inlet temperature and pressure) for the heat exchanger design. The diffuser sections were sized such that the inlet Mach number to the heat exchanger was fixed at 0.1. The diffuser walls were assumed adiabatic, although a practical design may include some cooling to control material temperatures. Significant cooling of the D_2 gas in the diffuser section introduces some problems to be discussed later. The length* of the supersonic section of an adiabatic diffuser is about 5 to 10 diameters for M_2 between 1 and 2 (Ref. 6). The diffuser diameter is the same as the target area diameter. The length of the subsonic diffuser section, based on a 3° half-angle with the exit Mach number equal to 0.1, ranged from 0.15 to 0.5 meter.

2. Heat Exchanger Dimensions and Performance

Figures 20 and 21 show heat exchanger dimensions as a function of target area inlet and exit Mach numbers. The heat exchanger was sized to remove the total of pumping power and beam energy (300 kW) not accounting for the assumed (~25%) cooling of the gas during and after compression. For the assumed $M_1 = 6$ and $M_2 = 1.7$, the heat exchanger is about 13 m long x 0.035 m (1.4 in.) i.d. on the gas side and 0.074 m (2.9 in.) o.d. on the water side (inside diameter of outer concentric pipe). The static pressure loss is about 28 kPa (4 psi),

* This is the minimum length, corresponding to the length of the "pseudo shock" (see p. 13).

Fig. 20. Heat exchanger length.

Fig. 21. Heat exchanger diameter.

Fig. 22, and the exit pressure is 1.12 MPa (162 psia). The exit pressure is not significantly affected by changes in M_2 (and f), Fig. 23. The effect of target gas density on the heat exchanger design is shown in Figs. 24 through 27. The design and performance are not very sensitive and changes in target operating density can probably be compensated for by adjustments in water flow, water inlet temperature and/or D_2 exit temperature.

3. Consideration of Boiling versus Nonboiling Design

Design of the heat exchanger with the assumption of no boiling is conservative from the standpoint that surges in the D_2 flow, inlet temperature, or water pressure, would not result in film boiling or burnout with possible damage. Figures 28 and 29 show, respectively, the maximum wall temperature and heat flux, q'' (at D_2 inlet). For $M_1 = 6$ and $M_2 = 1.7$, the maximum wall temperature is 520 K (476°F). This is well within the capability of chrome-nickel alloy steels. If the water pressure is set to correspond to the saturation pressure at the maximum wall temperature (about 3.8 MPa or 550 psia in this case), any reasonable increase in

D_2 exit temperature, flow rate, or local film coefficient will only cause local nucleate boiling on the water side. The heat exchanger could have been made shorter--thus cheaper--by designing it for nucleate boiling under normal operation. Aside from the reduced safety margin, the gas side would have to be internally finned to take full advantage of the high boiling heat transfer coefficient--thus tending to offset some of the cost advantage. The fins on the

Fig. 22. Heat exchanger static pressure loss.

Fig. 23. Heat exchanger exit pressure.

Fig. 24. Effect of target gas density on heat exchanger diameter.

Fig. 27. Effect of target gas density on heat exchanger exit pressure.

Fig. 25. Effect of target gas density on heat exchanger length.

Fig. 28. Maximum heat exchanger wall temperature (assumes no boiling).

Fig. 26. Effect of target gas density on heat exchanger pressure loss.

Fig. 29. Maximum local heat flux in heat exchanger.

gas side would be needed because only about 15% of the overall heat transfer resistance is on the water side--thus the length reduction would only be significant if both the water and gas side heat transfer were improved. Adding the fins (with a length reduction) will cause a net increase in the static pressure loss (see the Appendix) which, coupled with the preceding, argues strongly for the conservative nonboiling design.

4. Cooling Diffuser Sections

Diffuser sections will probably require cooling to maintain the walls within acceptable temperature limits. Uncooled walls would reach the gas stagnation temperature (Fig. 15) and at the higher Mach numbers, would require refractory metals. Inconel 718 would be an excellent choice for temperatures up to 1000 K (1350°F), but cooling the walls would likely be a more practical and less expensive alternative. In the absence of shock waves (Rayleigh flow) cooling a supersonic flow increases the Mach number and there is an upper limit to the amount of cooling that can take place (Fig. 30). The actual character of the shock structure in the duct will be oblique rather than normal. The shock structure has been termed a "pseudo shock" by Crocco⁸ and the following summary is a quotation.

Fig. 30. Maximum cooling of frictionless supersonic flow in constant area duct without shock.

"Only for low supersonic velocities and thin boundary layers is a quasi-normal shock possible in a duct. Otherwise, a more complicated, non-one-dimensional pattern is produced, which, for sufficiently high Mach numbers becomes multiple. Above a certain boundary layer thickness this multiple pattern contains only oblique shocks crossing the center of the duct and reflected back and forth in the central supersonic region of flow, while the region adjacent to the wall is adjusted gradually to the pressure increase. This adjustment is produced by strong exchanges of momentum due to the high turbulence generated in these regions ... the corresponding frictional effects are small and therefore the relations between the initial supersonic conditions and the final subsonic conditions are very close to those derived on assumptions that are exactly true only for a normal shock."

When the pressure gradient that the boundary layer must adjust to is high enough (about 2) separation occurs, with a recirculation "bubble" adjacent to the wall, bounded by a shear layer. The structure of these cavities is dominated by the interplay of constraints at the separation and recompression points, with matching of the inviscid and viscous flow along the dividing streamline.

There is no unifying theory and the existing models are insufficiently refined to explain many of the observable phenomena.⁹ There is no reliable framework for dealing with heat transfer. Heat transfer problems are still mainly a question of judicious interpretation of experimental data.

In spite of the pitfalls, we will try to place some estimate on the effects of cooling the supersonic flow. Reference 10 suggests that the local heat transfer coefficient (for turbulent separated flow over a flat plate at $M = 6$) is a factor of 2 greater than the corresponding flow with no separation. A large body of evidence^{6,7,8} concludes that (for adiabatic flow, at least) the static pressure rise in a duct with supersonic inlet and subsonic exit is close to that of a normal shock. We will assume that the heat transfer coefficient is equal to twice that with no shocks (and no boundary separation). The flow is accelerated from $M_2 = 1.7$ to M_3^* along the Rayleigh line while cooling and subsequently a normal shock (series of pseudo-shocks) reduces M_3^* to M_3 . The actual behavior will involve simultaneous cooling with a series of oblique shocks all along the tube, rather than sequentially as described. Figures 31 through 33 show T_{03} , P_3/P_2 , and M_3 versus the heat removal rate, Q . For these conditions, the length of the cooled section is approximately 1 meter per 100 kW of cooling, the final subsonic Mach number is between 0.4 and 0.6, and the maximum duct wall temperature is about 1170 K*.

The length of the subsonic heat exchanger is not greatly reduced by the additional cooling in the diffuser section. The reason is that the majority of the length is devoted to cooling the gas over the bottom end of the temperature range. The length needed for removing the first 100 kW is about 1/10 the length for removing the last 100 kW.

In this study no attempt was made to trade off the heat exchanger's capital cost with operating costs as was done in Ref. 1. This is because: (1) the cost of the heat exchanger is a very small fraction of the total capital cost of the INS;

* Assuming nonboiling. Lower temperatures are possible with selection of water pressure to allow boiling. Insulating the diffuser wall with, for example, concentric tubes of thin sheet tantalum on the i.d. would also be a solution, and might be necessary.

Fig. 31. Cooled supersonic diffuser exit temperature.

Fig. 32. Static pressure ratio.

Fig. 33. Mach number after cooling along Rayleigh line (no shocks).

(2) the conditions under which the heat exchanger must operate are not now well determined and may fluctuate with varying experimental conditions and/or design changes in the rest of the INS; and (3) the pressure loss in the heat exchanger is a negligible contribution to the overall system pressure ratio and operating cost. There was an attempt to maintain the heat exchanger exit pressure as high as possible, to maintain temperature levels within the acceptable range for common metals, to keep the design simple to fabricate, and to have sufficient design margin to accommodate changes in upstream conditions or transients.

D. Recommendations and Conclusions

Feasibility of the INS, as presently envisioned, depends heavily on the supersonic nozzle and target area's performance. Questions that should be resolved by detailed analysis and experiments include:

- Amount of deuterium leakage into the ion beam tube.
- Choking limits for deuterium flow based on ion beam power, nozzle-duct geometry, and flow conditions.
- Pressure recovery in target and diffuser.
- Electron-ion collision rate versus D-T reaction rate and impact on the above.

In particular, for the final design of the deuterium loop, two-dimensional viscous flow calculations should be made for the entire nozzle-target-diffuser system. Boundary layer development will affect each of the above, and cannot be prevented regardless of the type of supersonic nozzle.

The mechanical design of the system should consider carefully the detailed temperature distributions and thermal stresses, which could be quite severe.

The reference conditions for the D-T loop listed in Table I are only one of many possible alternatives, but are representative of what may be expected. It is hoped that further studies will provide a better basis for selecting design parameters for the system, which will be the subject of future work.

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NOMENCLATURE

P	pressure
T	temperature
A	area
M	Mach number
A	area
d	diameter
P	power
$\dot{Q}$	heat transfer rate
h	enthalpy
V	velocity
$\dot{m}$	mass flow rate
ρ	density
μ	viscosity
γ	specific heat ratio
β	beta factor
α	thermal expansion coefficient
ϵ	emissivity
σ	Stefan-Boltzmann constant

q	heat addition per unit mass	γ	ratio of specific heats
u, U	radial velocity	<u>Subscripts</u>	
ρ	density	0	stagnation condition
ϵ	mass addition per unit area per unit time	1, 2, etc.	station defined by Fig. 1
σ	momentum addition per unit area per unit time	<u>Superscript</u>	
		*	signifies sonic condition.

APPENDIX

USE OF INTERNAL FINS IN HEAT EXCHANGER

N fins of arbitrary shape.

Flow area = A, Perimeter = S.

Surface area = S dx.

$$\eta = \text{fin effectiveness} = \frac{q \text{ (actual } T_{fin})}{q \text{ (} T_{fin} = T_w)} < 1.$$

The energy equation is:

$$A \rho V C_p dT_0 = \eta h S dx (T_w - T_{aw}).$$

Assume adiabatic wall temperature, T_{aw} , equals stagnation temperature, T_0 . This is equivalent to a recovery factor $r_c = 1$. The actual value of r_c is about equal to $\sqrt{P_r}$.

Then,

$$\frac{dT_0}{T_w - T_0} = \frac{h S dx}{A \rho V C_p} \eta.$$

Using the Reynolds analogy (the error is small, $\sim 4\%$, for P_r close to 1)

$$\frac{h}{\rho V C_p} = \frac{f}{2}.$$

Then,

$$\frac{dT_0}{T_w - T_0} = \frac{\eta f S}{2A} dx$$

and

$$\frac{\eta}{2} \left[\frac{f S}{A} (x_2 - x_1) \right] = \ln \frac{T_w - T_{01}}{T_w - T_{02}}.$$

If $r_c \neq 1$, constants would enter on the right side. The quantity in brackets on the left is the friction coefficient, C_f . Approximation to the pressure drop equation:

$$-dp = \frac{G^2 \bar{v}}{2} dC_f + G^2 dv,$$

$$dC_f = \frac{f S}{A} dx$$

$$P_1 - P_2 = G^2 \left[1/2 \bar{v} C_f + v_2 - v_1 \right]$$

$$v = \frac{RT}{P}$$

$$P_1 - P_2 = G^2 R \left[\frac{C_f}{4} \left(\frac{T_1}{P_1} + \frac{T_2}{P_2} \right) + \frac{T_2}{P_2} - \frac{T_1}{P_1} \right]$$

$$P_2^2 - P_1^2 \left[P_1 - \frac{G^2 RT_1}{P_1} \left(\frac{C_f}{4} - 1 \right) \right] + G^2 RT_2 \left(\frac{C_f}{4} + 1 \right) = 0$$

$$\gamma M^2 = \frac{v_1^2}{RT_1} = \frac{G^2 RT_1}{P_1^2}$$

$$P_2^2 - P_1^2 B P_2 + P_1^2 E = 0.$$

Where:

$$B = 1 - \gamma M_1^2 (C_f/4 - 1)$$

$$E = \gamma M_1^2 \frac{T_2}{T_1} (C_f/4 + 1)$$

$$\frac{P_2}{P_1} = 1/2 \left[B + \sqrt{B^2 - 4E} \right].$$

All except C_f in the above are fixed by flow conditions and not geometry. From the energy equation:

$$C_f = \frac{2}{\eta} \ln \frac{T_w - T_{01}}{T_w - T_{02}}.$$

The value of C_f is fixed by the energy equation, and is increased by the fin effectiveness < 1 . If $\eta = 1$, then length can be reduced by adding fins, and the pressure drop remains constant. Since η is

never 1 for actual fins, we conclude that adding fins hurts ΔP .

In practice, fins are usually used where:

1. Heat exchanger weight and/or volume are of extreme importance (aircraft and spacecraft).
2. Pumping power on the gas side is "free" or of smaller importance (automotive radiators, industrial applications, air conditioners).

3. The gas h is $\sim 1/100$ to $1/50$ the liquid h (air to water). In this case, D_2 is a very good heat exchange medium and the gas h is about $1/4$ the nonboiling water h .

Since minimum ΔP is of prime importance for this application, internal fins on the gas side are not desirable.