*J. Fluid Mech.* (1967), *vol.* 27, *part* 3, *pp.* 503–512

*Printed in Great Britain*

# Temperature measurements in a hypersonic gun tunnel using heat-transfer methods

By B. E. EDNEY
The Aeronautical Research Institute of Sweden (FFA)

(Received 20 October 1965)

The theory of Fay & Riddell (1958) is used to calculate stagnation temperatures from stagnation-point heat-transfer rates measured in the working section of a hypersonic gun tunnel at a Mach number of 9·8. Measurements using both thin-film gauges and calorimeters are described. The temperatures measured using this technique are found to be lower than predicted by Lemcke (1962) from measurements of shock strengths and final pressures in the gun barrel. This discrepancy is attributed to heat losses in the barrel during the initial shock compression cycle. A simple theory is developed to take into account these losses. There is good agreement between this theory and the experimental results.

## 1. Introduction

As first conceived the hypersonic gun tunnel promised several advantages over the conventional shock tunnel. Among these were effective separation of driver and driven gases, higher stagnation pressures (several hundred atmospheres) and hence higher Reynolds numbers, longer testing times ($\sim$ 30 msec) and yet comparable stagnation temperatures. Thus, for temperatures in excess of 3200 °K, it is possible to produce dissociation in the test section without dissociation in the stagnation chamber and the nozzle.

Unfortunately it soon became evident that the theoretical temperatures could not be reached. Consequently most tunnels are operated 'cold', i.e. at stagnation temperatures near to 1000 °K, sufficient to avoid condensation at Mach numbers between 8 and 10. At the same time many of the problems associated with piston design are avoided, since we do not need to go to pressure ratios greater than 100, using nitrogen driving air. The pressure ratio, i.e. the pressure in the driver gas to the initial pressure in the driven gas, is an important parameter in gun tunnel operation. Theoretically, at least, we may increase the stagnation temperature in the working gas by increasing the pressure ratio. Thus Cox & Winter (1961) have measured temperatures ranging from 800 °K, at a pressure ratio of 50, to 1400 °K, at a pressure ratio of 250. These temperatures were calculated from measured blowing times and also from flow velocity measurements in the test section; both techniques yielding similar results. At still higher pressure ratios the design of a suitably light, yet strong piston becomes extremely difficult. For a detailed discussion of the several factors influencing piston design and some of the practical problems the reader is referred to Lemcke (1962) and Brown-Edwards (1965*b*).

Again we may avoid some of these problems by pre-heating the working gas. This is the technique employed at R.A.R.D.E. Thus, by heating the barrel to 250 °C, temperatures in excess of 2500 °K have been measured. Obviously a hot barrel presents some practical problems, too. At FFA much work has gone into refining the piston design and it is now possible to drive at pressure ratios up to 1500. Earlier measurements by Lemcke (1962), based on both blowing times and pressures measured in the barrel, indicated temperatures between 2500 and 2800 °K at a pressure ratio of 1000. However, subsequent sodium-line reversal measurements by Brown-Edwards (1965*b* and the preceding paper) suggested much lower temperatures, around 2100 °K.

The purpose of the present investigation was to resolve this discrepancy by making heat-transfer measurements in the test section, hence providing an independent measurement of the stagnation temperature; and to examine in more detail the heat losses in the barrel, which had previously been neglected, to see how these limit gun tunnel performance.

## 2. Calculation of stagnation temperatures from measured stagnation-point heat-transfer rates

Grabau, Smithson & Little (1964) have successfully measured stagnation temperatures in the range 1500 to 5000 °K, in nitrogen, in a hot shot tunnel, using this method, based on the theory of Fay & Riddell (1958). It was suggested that this would be a suitable method for use in the gun tunnel.

We shall assume that the flow is undissociated and that the heat-transfer rate at the stagnation point of an axisymmetric blunt body is given by

$$q = 0{\cdot}76\sigma^{-0{\cdot}6}(\rho_w\mu_w)^{0{\cdot}1}(\rho_s\mu_s)^{0{\cdot}4}(h_s-h_w)(du/dx)_s^{\frac{1}{2}}, \tag{1}$$

where $\sigma$ is the Prandtl number, $\rho$ denotes density, $\mu$ viscosity and $h$ enthalpy and the suffixes $w$ and $s$ denote conditions at the wall and at the edge of the boundary layer respectively.

For a hemisphere in a hypersonic flow the velocity gradient at the stagnation point is given by (see, for example, Korkan 1962)

$$(du/dx)_s = R^{-1}[2(p_s-p_\infty)/\rho_s]^{\frac{1}{2}}, \tag{2}$$

$$\simeq R^{-1}(2p_s/\rho_s)^{\frac{1}{2}}, \quad p_s \gg p_\infty. \tag{2a}$$

Equation (1) then reduces to

$$q = 0{\cdot}76\sigma^{-0{\cdot}6}(\rho_w\mu_w)^{0{\cdot}1}(\rho_s\mu_s)^{0{\cdot}4}(h_s-h_w)R^{-0{\cdot}5}(2p_s/\rho_s)^{0{\cdot}25}. \tag{3}$$

Clearly (3) can be expressed in the form

$$q = F(\sigma, R, p_s, T_s, T_w), \tag{4}$$

where $T$ denotes temperature. In expressing (3) in this form we make use of tables of thermal properties of gases; i.e. it is a purely numerical formulation. Now $\sigma$ and $R$ are known and $T_w$ is measured during the run. $p_s$ can also be measured directly during the run, but it is more convenient in the present case to monitor the pressure in the stagnation chamber and determine $p_s$ from a previous calibration. Finally, knowing the other variables we find a value of $T_s$ to match the right-hand side of (4) with the measured $q$, using an iterative process or previously prepared graphs.

The stagnation-point heat-transfer rates were measured using both thin-film gauges and calorimeters, as described in the following sections.

## 3. Measurements using thin-film gauges

The models used were 10 mm diameter hemispheres of Jenaer Duran 50 (Pyrex-type glass) with platinum thin-film gauges mounted at the stagnation point. The heat-transfer rates were determined directly from the surface temperature by means of a T-section analogue network, of the type developed by Meyer (1963), designed to have a response time of 50 $\mu$sec and a total testing time of 50 msec. The thin-film gauges were calibrated to determine the value of $(\rho ck)^{\frac{1}{2}}$ for Duran 50 using the method devised by Skinner (1962). Since the gauges were uncoated, Dow Corning silicon fluid '200' was used instead of water as the reference liquid. As a further check on the complete system, heat-transfer measurements were made in a continuous hypersonic tunnel, where the temperature and pressure were known very accurately, using the same probes and instrumentation to be used in the gun tunnel. A pneumatic device, developed for earlier heat-transfer experiments by Thomann (1965), was used in conjunction with a shield and splitter plate (figure 1, plate 1) to inject the models into the tunnel and withdraw them quickly to avoid damage. Figure 2, plate 2, shows the output recorded using this technique. The spike on the record is due to the shock from the lip of the splitter plate impinging on the model. The measured heat-transfer rates were within 5 % of the expected values. This technique could be used for calibration, overcoming many of the disadvantages associated with electrical calibrations discussed by Edney (1964).

All measurements in the gun tunnel were made at $M = 9{\cdot}8$, to ensure the highest heat-transfer rates consistent with adequate running times at the higher pressure ratios. Corrections were applied to account for the finite dimensions of the film, since the heat-transfer rate decreases away from the stagnation point. These were small. Corrections were based on data given by Kemp, Rose & Detra (1959). Larger corrections, the order of 10 %, were necessary to account for the variation of the thermal properties of the gauge-backing material with temperature, surface temperatures exceeding 100 °C under certain conditions. These corrections involve approximate solutions of the conduction equation and are discussed by Edney (1965) and Walenta (1964). Assuming a variation in the thermal conductivity of the backing material of the form

$$[k(T)]^{\frac{1}{2}} = [k(T_0)]^{\frac{1}{2}}(1+\beta T), \tag{5}$$

it may be shown that the surface temperature of the gauge with variable thermal properties, $T_v$, is related to the surface temperature of the gauge with constant thermal properties, $T_c$, by the expression

$$T_v(1+\beta T_v) = T_c. \tag{6}$$

This allows us to calculate a correction to the output of the analogue network, which should 'see' $T_c$ instead of $T_v$ as is actually the case. This is fairly straightforward for a constant heat-transfer rate. For variations in the heat-transfer rate with time a correcting network of the type developed by Walenta (1964) would have been necessary.

The accuracy of the measurements was impaired by erosion of the thin-film gauges, due to particles in the working gas, probably from the piston. A variety of possible remedies were tried without much success. The erosion is seen as a change in the cold resistance at the start and end of the run—exactly where in the run this occurs is not known. An analysis of the uncertainty this change in resistance would introduce shows that the measured heat-transfer rate exceeds the true heat-transfer rate, if we reduce the results using the resistance at the start of the run. This uncertainty varied from approximately $+5\%$, at the lowest pressure ratios, to approximately $+10\%$, at the higher pressure ratios where erosion was most severe. This agrees reasonably well with the observed divergence between results obtained with thin-film gauges and results obtained with calorimeters. A typical oscilloscope record obtained in the gun tunnel is shown in figure 3, plate 3. The results of measurements in the gun tunnel are plotted in figure 5.

## 4. Measurements using calorimeter gauges

Two types of calorimeter were considered. The first is an element of copper or platinum mounted on an insulating backing. This type has been used successfully in shock tunnels. However, for longer testing times, as in the gun tunnel, conduction losses to the backing material can become appreciable and large corrections must be applied. Moreover, the corrections are difficult to calculate, although an analogue device to compensate automatically for these losses is outlined by Edney (1965). The corrections are often inaccurate if the calorimeter and the backing are not in good thermal contact. The second alternative is the thin shell calorimeter. This was the type finally adopted. The calorimeter consists of a hemispherical copper shell, 0·1–0·3 mm thick and 10 mm in diameter. The temperature is measured in the stagnation point using a single constantan wire of 0·12 mm diameter, soldered into a 0·15 mm hole, the copper shell forming the other half of the thermocouple.

Corrections were applied to account for the tangential conduction losses away from the stagnation point. Assuming a constant heat-transfer rate, $q$, and a variation in heat transfer with angular displacement $\theta$ from the axis of the form $q\cos\theta$, the measured temperature variation with time is

$$\left(\frac{dT}{dt}\right)_s = \frac{q}{a\rho s}\exp\left(-\frac{2kt}{R^2\rho s}\right), \tag{7}$$

$$\simeq \frac{q}{a\rho s}\left[1-\frac{2kt}{R^2\rho s}\right], \tag{7a}$$

where $a$ is the thickness of the shell, $\rho$ the density, $s$ the specific heat, $k$ the thermal conductivity and $R$ the radius. The second term, $2kt(R^2\rho s)^{-1}$, represents the first-order correction for tangential conduction losses.

The uniformity of thickness of the shell was measured using a micrometer at various points on the shell and cross-checked by weighing each shell and calculating an average shell thickness. A number of shells were manufactured and the best selected for the tests (departure from uniform thickness $\pm 2\%$). The thermocouples were calibrated, after mounting in the model, over a range of 10–150 °C and compared with the manufacturer's calibration. The main source of error was in measuring the slope of the temperature/time curve during a run. This was

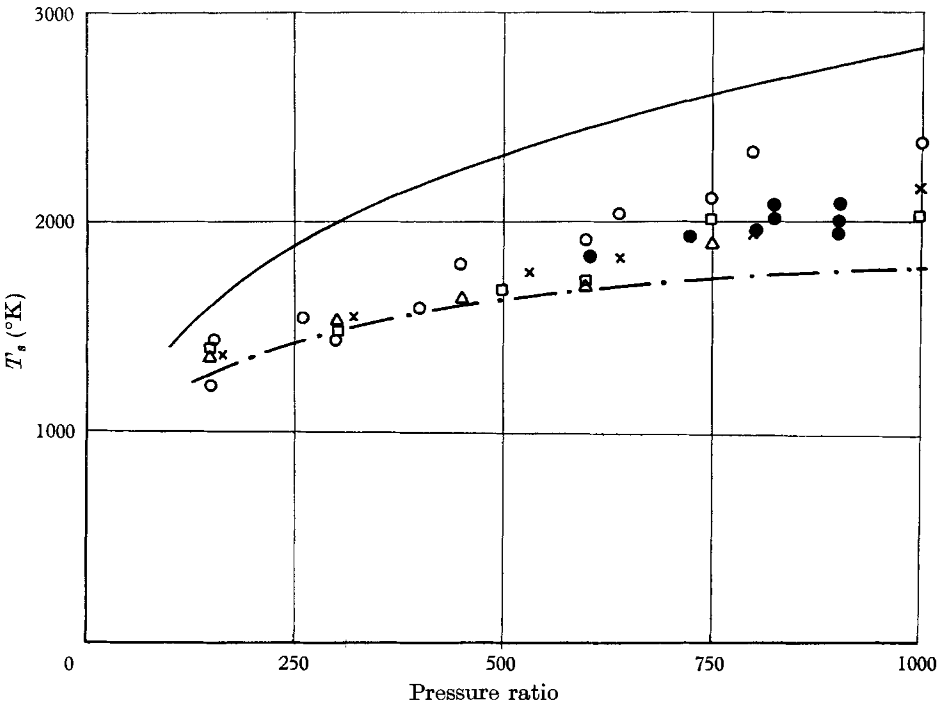


FIGURE 5. Results of measurements using various techniques compared with theoretical temperature variation. $P_0 = 150$ atm. ○, thin-film gauge; □, hemispherical calorimeter (0·32 mm thick); △, hemispherical calorimeter (0·11 mm thick); ×, flat-faced cylinder calorimeter (Edney 1965); ●, sodium-line reversal (Brown-Edwards 1965*b*); —— pressure history, no correction; —·— pressure history, corrected for heat losses.

approximately $\pm 2\%$, varying slightly with running conditions and the ambient noise level. The accumulated errors amounted to about $\pm 5\%$ in the measured heat-transfer rate. Assuming that the Fay & Riddell theory introduced no further error it was estimated that the measured temperatures were accurate to $\pm 50$ °C at the lower pressure ratios rising to $\pm 80$ °C at the higher pressure ratios. This is appreciably better than could be obtained with the thin-film gauges under the same conditions.

A typical oscilloscope record obtained using a shell calorimeter is shown in figure 4, plate 4. The results of the tests are presented in figure 5. Some scatter in the results is attributable to variable diaphragm opening, which is equivalent to small variations in the pressure ratio for a fixed diaphragm opening.

## 5. Estimation of heat losses during the initial shock compression cycle

Figure 6 represents an idealized $(x, t)$-diagram for the shock and piston motion. The time, $\tau_1$, for the initial shock to reach the end wall varies between 5 and 8 msec, for the 6 m long barrel, depending on the pressure ratio. The tunnel starts about 5 msec later, at time $\tau_2$. The working gas is heated by multiple shock reflexions between the end wall and the piston. In calculating the final temperature of the working gas it is sufficient (Lemcke 1961, 1962) to consider only the first three shocks and assume isentropic compression thereafter to the final stagnation pressure. It is evident that, if the average temperature, $T_2$, behind the first shock

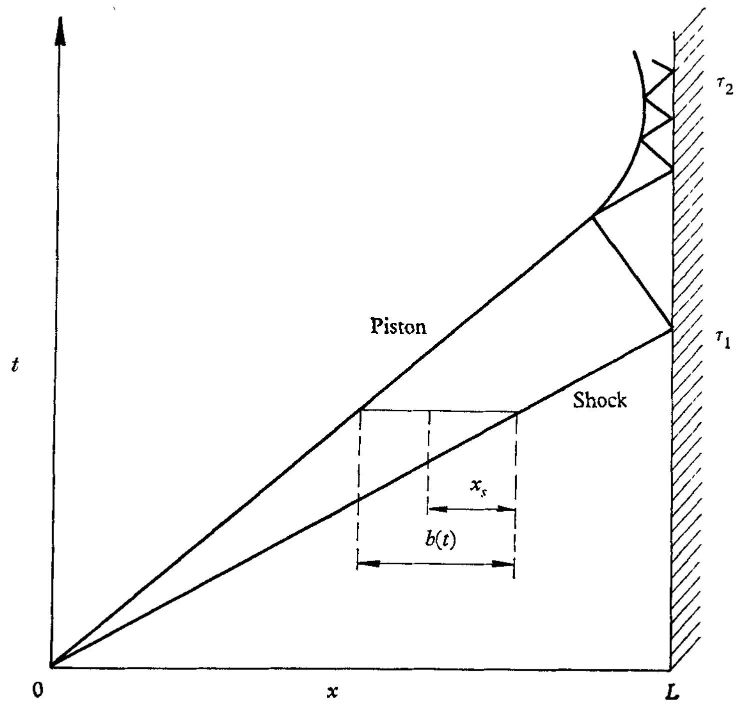


FIGURE 6. An idealized $(x, t)$-diagram for the shock and piston motion.

is lowered, on account of heat losses to the barrel walls, the final temperature will be lowered proportionally, neglecting any further losses. The heat-transfer rate to the wall increases with each shock reflexion, as the temperature of the working gas increases, but the integrated heat losses, on account of the longer travel time and greater surface area behind the first shock, are sufficiently large so that the relative temperature drop $\Delta T_2/T_2$ is greatest behind the first shock and dominates all other losses.

Let us now estimate $\Delta T_2/T_2$. We shall assume that the shock and piston travel along the barrel with constant velocities. This is a poor assumption at the beginning of the motion. However, the exact shock wave and piston paths calculated by Lemcke (1962) for finite piston mass and some measurements of the piston motion using a micro-wave tracking technique (Brown-Edwards 1965*a*) suggest that this is an otherwise reasonable assumption, since a light piston (8 g) is accelerated to a steady velocity in less than 1 m (i.e. less than 20 % of the barrel length in the present case).

A further assumption is that the heat-transfer rates to the wall, aft of the shock, are the same in the gun tunnel as in a shock tube; since we shall need to use data obtained in a shock tube by Hartunian, Russo & Marrone (1960). This cannot be so in the regions immediately in front of the piston and the contact surface. In the gun tunnel the boundary layer is scooped up by the piston and mixed with the hot core and in the shock tube the boundary layer is continuous through the contact surface, which is distorted. However, we shall assume that this mixing region is small compared with the shock–piston separation. Preliminary measurements, part of a current programme to study these differences in more detail, show that the piston–shock separation is smaller than the calculated value, partly on account of a pressure gradient between the shock and the piston (between 30 and 60 % rise in static pressure, increasing with increasing pressure ratio) and partly because of a temperature gradient resulting from heat losses from the boundary layer. The heat-transfer rates to the wall are found to be slightly higher in the gun than in the shock tube, for the same shock speed. The shorter separation and higher heat-transfer rates tend to cancel each other out. The significance of this will be clearer from what follows.

Let the heat-transfer rate at some point distance $x_s$ behind the shock be $q(x_s)$. Then the heat lost per second from the boundary layer at time $t$ is

$$Q(t) = 2\pi r \int_0^{b(t)} q(x_s)\,dx_s, \tag{8}$$

where $r$ is the radius of the barrel and $b(t)$ the shock–piston separation. Hence the total heat lost in time, $\tau_1$, for the first shock to reach the end wall is

$$H(\tau_1) = 2r \int_0^{\tau_1} \int_0^{b(t)} q(x_s)\,dx_s\,dt, \tag{9}$$

$q(x_s)$ will depend on the nature of the boundary layer. Side-wall-mounted thin-film gauges indicate that the boundary layer is turbulent in the gun, in agreement with measurements made by other workers. For a turbulent boundary layer Hartunian, Russo & Marrone find that

$$St(Re)^{\frac{1}{5}} \doteqdot 3\cdot7 \times 10^{-2} = k_1, \tag{10}$$

where

$$St = q(x_s)/\rho_e \mu_e (h_r - h_w), \tag{11}$$

$$Re = \rho_e u_e x_s/\mu_e, \tag{12}$$

$\rho_e$ = density behind the shock,

$u_e$ = velocity behind the shock relative to the wall,

$h_e$ = enthalpy behind the shock,

$h_r$ = recovery enthalpy = $c_p T_r$,

$$= h_e[1 + u_e^2(2h_e)^{-1}\,\mathrm{Pr}^{[0\cdot39 - 0\cdot023 u_s/(u_s - u_e)]}], \tag{13}$$

$h_w$ = wall enthalpy,

$\mu_e$ = viscosity behind the shock.

Substituting into (9) from (11) and (12) we get

$$H(\tau_1) = \tfrac{25}{18}\pi r k_1 \rho_e^{\frac{4}{5}} u_e^{\frac{4}{5}} (u_s - u_e)^{\frac{4}{5}} \mu_e^{\frac{1}{5}} (L/u_s)^{\frac{9}{5}} (h_r - h_w). \tag{14}$$

Now

$$\Delta T_2 = H/mc_p, \tag{15}$$

$$= Hu_s/c_p \pi r^2 L\rho_e(u_s - u_e), \tag{15a}$$

where $m$ is the total mass of the working gas and $c_p$ the specific heat at constant pressure.

Thus

$$\frac{\Delta T_2}{T_2} = \tfrac{25}{18} k_1 r^{-1} \left(\frac{Lu_e}{u_s}\right)^{\frac{4}{5}} \left(\frac{\mu_e}{\rho_e(u_s - u_e)}\right)^{\frac{1}{5}} \left(\frac{T_r - T_w}{T_2}\right). \tag{16}$$

We see from (16) that the fractional loss is roughly proportional to the length of the barrel and inversely proportional to the radius. Losses also increase with increasing shock speed. Reducing the initial pressure in the driven gas, i.e. reducing the density, also increases the losses. The combination of higher shock speeds and lower pressures in the driven gas means that the losses increase very rapidly with increasing pressure ratio, for a fixed driving pressure. Values of $\Delta T_2/T_2$ of the order of 35 % are reached at the higher pressure ratios in the FFA gun tunnel. Equation (16) has been used to estimate the correction to the final stagnation temperature, neglecting all other losses, in the FFA gun tunnel.

## 6. Discussion of results

The full curve in figure 5 shows the temperatures calculated from measured shock strengths and final pressures at the end of the barrel, neglecting all heat losses, as a function of pressure ratio. The broken curve shows the temperatures calculated from the same experimental data but with the correction for the heat losses applied, using (16). The agreement with the temperatures calculated from the stagnation-point heat-transfer experiments is remarkably good considering the crudeness of the physical model.

Ignoring the thin-film measurements, which we know to overestimate the final temperature, the agreement between the corrected theoretical curve and the measured temperatures is within the limits of experimental accuracy, for pressure ratios below 500. Above a pressure ratio of 500 the measured values lie above the corrected theoretical curve. This discrepancy cannot be attributed to experimental error, both the sodium-line reversal and calorimeter measurements giving closely agreeing results. It is thought instead to be due mainly to the combustion of Makrolon piston material with oxygen in the working gas, pitting and burning of the piston occurring for pressure ratios above 500, which raises the temperature of the working gas. This combustion heating has been demonstrated under different running conditions, using both pure nitrogen, which inhibits piston combustion, and nitrogen/air mixtures (Edney 1966). However, it must also be expected that the simple model we have set up to calculate the heat losses is less good at the higher pressure ratios, when the heat losses are large and the boundary layer ahead of the piston fills the whole barrel. In addition the acceleration phase is longer at the higher pressure ratios and hence the assumption of uniform shock speed, equal to the maximum measured shock speed, along the length of the barrel means that the heat loss calculated is slightly greater than the actual heat loss. However, errors in temperature measurement and other uncertainties in the analysis make further resolution of the losses difficult. A more refined theory might take into account the effects of the temperature and pressure gradients between the shock and the piston, due to the non-uniform motion of the piston and the heat losses to the wall. In this case the heat-transfer rate to the wall at a given distance, $x$, behind the shock is no longer a function of $x$ alone but also of the time, $t$. In addition, losses from the stagnant gas behind the first reflected shock should also be taken into account. However, both measurements in the FFA gun tunnel and rough calculations of these losses indicate only a small effect (a drop of $\sim 50$ °C) in the final temperature. Further refinements and corrections become complicated and obscure the simple physical picture presented here.

The results obtained using the thin-film gauges are also included in figure 5, although they are less accurate. The higher temperatures measured with this technique and the larger scatter are attributed to the erosion of the gauges, as explained earlier.

## 7. Conclusions

The experimental results presented here, together with the simple theory of boundary-layer losses, explain the lower temperatures compared with those originally predicted in the FFA tunnel and in similar facilities. It also accounts for the very low emission which hampered earlier efforts to measure the temperatures at the lower pressure ratios using the sodium-line reversal technique, since the temperature does not exceed 1600 °K below a pressure ratio of 500. In addition, the determination of temperatures from stagnation-point heat-transfer rate measurements has been demonstrated to be a useful technique under conditions where other more direct temperature measurement methods are unsuitable.

The discrepancy between the design and actual performance in the FFA gun tunnel is particularly marked, on account of the long barrel (6 m) and narrow bore (4 cm). Some improvement could be accomplished by running at higher absolute pressure levels but this is small. Thus increasing the driving pressure from 150 to 250 atm results in a final temperature only 50 °C higher, at the same pressure ratio. It would appear that we could halve the losses by using a shorter barrel ($\sim 3$ m) but this would be at the expense of reducing the running time. Thus at $M = 10$ the tunnel will not start at pressure ratios greater than 500, which defeats our purpose.

Another disadvantage with a short barrel is that the distance the piston takes to reach its final, steady velocity becomes an appreciable fraction of the total length. Lemcke (1961, 1962) has already noted that under such circumstances there exists a large drop in temperature between the shock and the piston. Following a suggestion by Berndt,† Lemcke (1963) pursued the idea of an evacuated acceleration section ahead of the piston. This acceleration section has been tried at the FFA with partial success. However, serious contamination from the intermediate diaphragm, reported by Brown-Edwards (1965*a*), limits its applica-

† Professor of Gas Dynamics, the Royal Institute of Technology, Stockholm.

tion at present. A more attractive arrangement would be a short ($\sim 3$ m), wider-bore ($\sim 10$ cm) barrel, with a short ($\sim 1$ m) evacuated acceleration section between the piston and the driven gas, assuming that some form of quick opening valve could be developed to replace the intermediate diaphragm. The wider bore would be necessary not only to reduce the heat losses to a minimum but also to preserve the running time at the high pressure ratios. However, such an arrangement would require major structural changes, at present impracticable. We mention this arrangement in that it represents a departure from conventional gun tunnel design. More efficient drivers have also been considered but even with helium driving into preheated air and using a lighter piston (4 g) calculations promise that losses will limit temperatures to under 3000 °K in the FFA gun tunnel. We have confined our attention almost exclusively to increasing temperatures by reducing heat losses and by more efficient design. Even so, it is doubtful whether temperatures in excess of 3000 °K will be reached in any gun tunnel facility without the additional help of pre-heating.

This work was sponsored by the Royal Swedish Air Force Board.

## REFERENCES

Brown-Edwards, E. 1965*a* *FFA Report* AU-II-182.
Brown-Edwards, E. 1965*b* *FFA Report* AE-194:1.
Cox, R. & Bowman, J. 1965 RARDE Private communication.
Cox, R. & Winter, D. 1961 *ARDE Report* (B) 9/61.
Edney, B. 1964 *FFA Report* AU-209:1.
Edney, B. 1965 *FFA Report* AU-209:2.
Edney, B. 1966 Paper presented at ICAS Congress, London, Sept. 1966.
Fay, J. & Riddell, F. 1958 *J. Aero. Sci.* vol. 25, no. 2.
Grabau, M., Smithson, H. & Little, W. 1964 AEDC-TDR-64-50.
Hartunian, R., Russo, A. & Marrone, P. 1960 *J. Aero. Sci.* **27**, no. 8.
Kemp, N., Rose, P. & Detra, R. 1959 *J. Aero. Sci.* **26**, no. 7.
Korkan, K. D. 1962 *Ars. J.* p. 1924.
Lemcke, B. 1961 *J. Aero. Sci.* **28**, no. 10.
Lemcke, B. 1962 *FFA Report* 90.
Lemcke, B. 1963 *ASRL Report*, no. 1010.
Meyer, R. 1963 *NRC Aero Report* LR-375.
Skinner, G. 1962 *Cornell Aero. Lab. Report* CAL-105.
Thomann, H. 1965 *FFA Memo* 39.
Walenta, Z. 1964 *UTIAS Tech. Note* no. 84.
Tables of thermal properties of gases, National Bureau of Standards Circular 564.

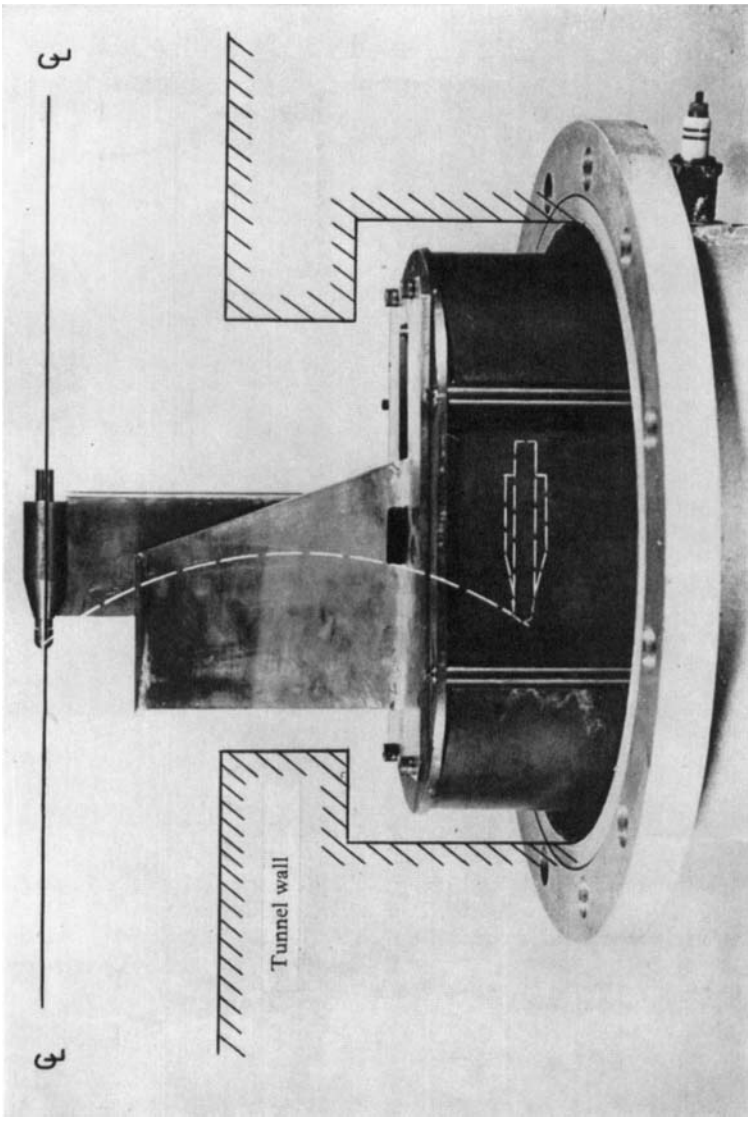


FIGURE 1. Hemispherical model mounted on injection device showing shield, splitter plate, and injection path.

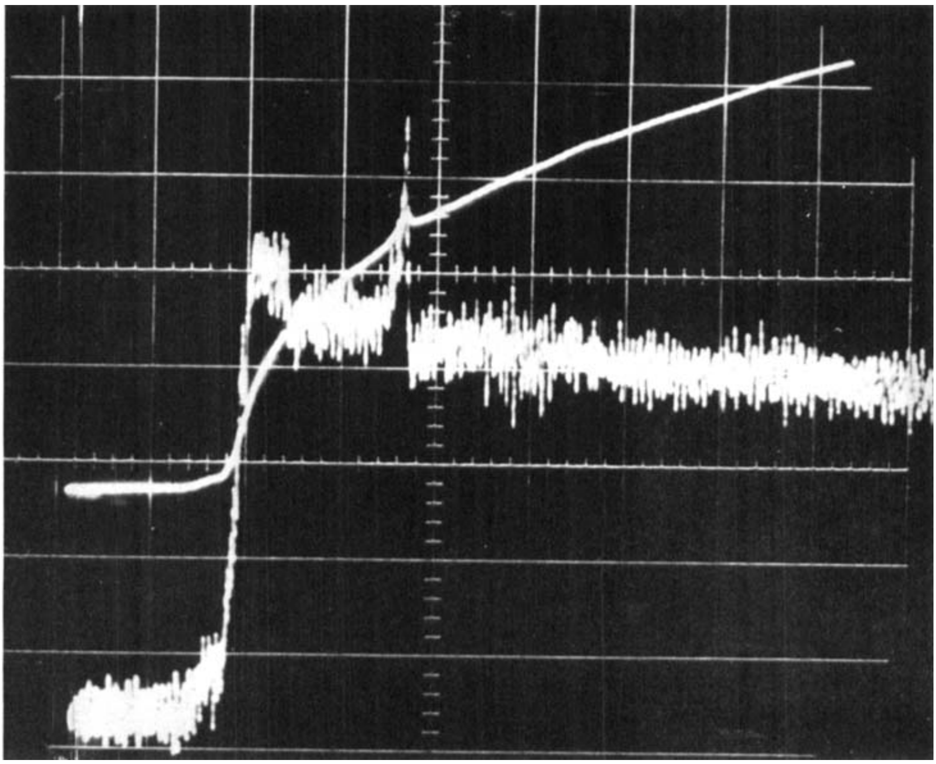

FIGURE 2. Oscilloscope record obtained using a thin-film gauge by injecting model into continuous tunnel. $M = 4{\cdot}6$, $T_s = 355\ ^{\circ}\mathrm{C}$, $P_0 = 11{\cdot}7$ atm. Upper trace: surface temperature 10 mV $\mathrm{cm}^{-1}$. Lower trace: heat-transfer rate 200 V $\mathrm{cm}^{-1}$. Sweep rate: 5 msec $\mathrm{cm}^{-1}$.

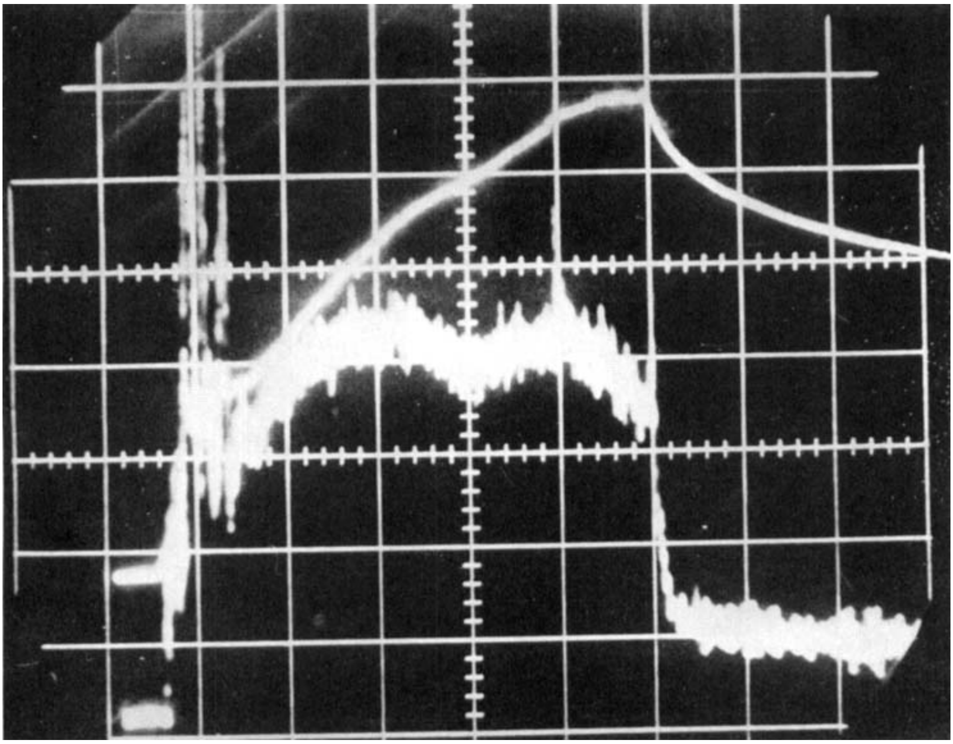

FIGURE 3. Oscilloscope record obtained using thin-film gauge in the gun tunnel. $M = 9{\cdot}83$, $P_0 = 150$ atm. Upper trace: surface temperature 20 mV $cm^{-1}$. Lower trace: heat-transfer rate 500 V $cm^{-1}$. Sweep rate: 5 msec $cm^{-1}$.

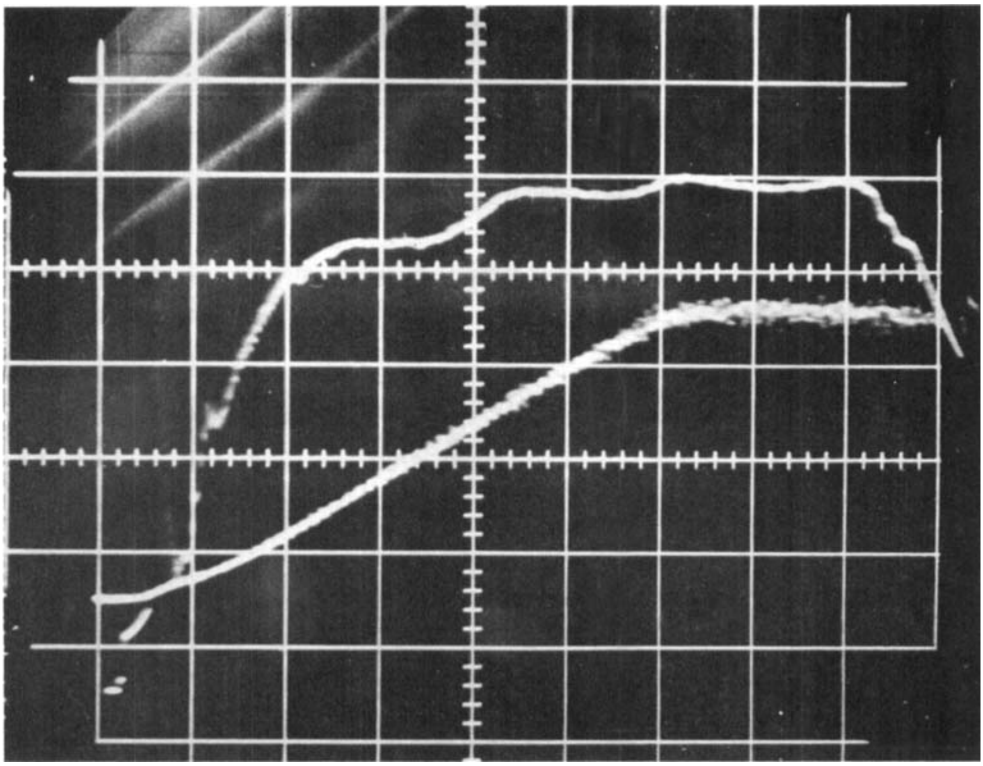

FIGURE 4. Oscilloscope record obtained using a hemispherical shell calorimeter in the gun tunnel. $M = 9{\cdot}83$, $P_0 = 150$ atm. Pressure ratio 300. Upper trace: thermocouple 1 mV cm$^{-1}$. Lower trace: stagnation chamber pressure. Sweep rate: 5 msec cm$^{-1}$.