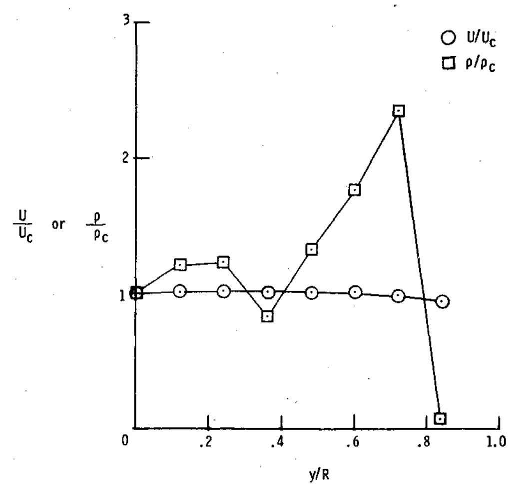

Fig. 2 Vertical velocity and static density profiles at the nozzle exit for CO_2 test gas ($R = 31.75$ cm).

operation.[1,2] Values of flow velocity at the two earliest times agree within the experimental uncertainty (denoted by barred symbols), whereby a decrease in velocity occurs between the earliest and the latest times for both test gases. Thus, the flow velocity is essentially constant during the later half of the quasisteady pitot-pressure period and decreases during the period of decreasing pitot pressure and high-frequency variation in pitot pressure. The flow velocity increases as it traverses the distance between the nozzle entrance and exit, this increase being 4 to 5% for air and 2% for CO_2. For hypersonic flow, the pitot pressure is approximately equal to ρU^2; hence, the results of Fig. 1 imply that freestream density and velocity are essentially invariant with time just downstream of the nozzle exit for about 300 μsec following the initial peak in pitot pressure. These results also confirm that the flow density undergoes an appreciable decrease in magnitude as the flow traverses the nozzle, with the density at the nozzle exit being about 0.02 times that at the nozzle entrance for both air and CO_2 test gases. (Inversely, the ratio of density at the nozzle entrance to that at the nozzle exit is approximately equal to the nozzle geometric area ratio.) The relatively small increase in flow velocity and large decrease in density as the flow traverses the nozzle is expected, since most of the flow energy is kinetic at the nozzle entrance, and the continuity of mass and energy relations show that the density will decrease as the nozzle area ratio increases.

The vertical profile of the axial component of flow velocity and the density is shown in Fig. 2 for CO_2 test gas. The velocity is nondimensionalized by measured velocity on the nozzle centerline over the interval 11.82 to 15.63 cm downstream of the nozzle exit, and the density also is nondimensionalized by the nozzle centerline value downstream of the nozzle exit. Velocity and density profiles were obtained during the period of quasisteady pitot pressure. A uniform core of the flow velocity (within 2%) exists about the nozzle centerline, the diameter of which is approximately 0.7 times the nozzle exit diameter. Outside of this core of uniform velocity, the flow velocity was observed to decrease rapidly in the direction of the nozzle wall. Values of freestream density are within 20% in a region about the nozzle centerline, and the diameter of this region is about half the nozzle exit diameter. A toroid of high-density flow surrounds this inner region of relatively uniform density, but the flow velocity is constant across this region and the toroid. Outside the toroid, the density diminishes rapidly in the direction of the nozzle wall. Vertical profiles of flow velocity and density for air were similar to those observed for CO_2. These findings are in agreement with previously measured pitot-pressure profiles,[1] which showed a relatively uniform pitot-pressure inner-core region surrounded by a region of higher pitot pressure. The diameter of this uniform pitot-pressure region was approximately 0.5 times the nozzle exit diameter for CO_2 and air test gases. A similar expansion tunnel flow model was observed in the Langley pilot model expansion tunnel.[5] The toroid of high-density flow is believed to be caused by shocks (weak disturbances), as predicted for hypersonic flow in conical nozzles.[6] As a point of interest, the value of centerline density obtained from measuring the charge collected by the ion probe is about twice the density inferred from the measured velocity and pitot pressure. In view of the assumptions required to obtain a value of density from the measured charge, this difference in centerline density of a factor of 2 is believed to be quite reasonable.

References

[1] Miller, C. G., "Operational Experience in the Langley Expansion Tube with Various Test Gases and Preliminary Results in the Expansion Tunnel," AIAA Paper, Arlington, Texas, June 7-9, 1976.

[2] Miller, C. G., "Expansion Tunnel Performance with and without an Electromagnetically Opened Tertiary Diaphragm," *AIAA Journal*, Vol. 15, No. 7, 1977, pp. 1045-1047.

[3] Trimpi, R. L. and Callis, L. B., "A Perfect-Gas Analysis of the Expansion Tunnel, a Modification to the Expansion Tube," NASA TR R-223, April 1965.

[4] Moore, J. A., "Description and Initial Operating Performance of the Langley 6-Inch Expansion Tube Using Heated Helium Driver Gas," NASA TM X-3240, Sept. 1975.

[5] Friesen, W. J. and Moore, J. A., "Pilot Model Expansion Tunnel Test Flow Properties Obtained from Velocity, Pressure, and Probe Measurements," NASA TN D-7310, Nov. 1973.

[6] Callis, L. B., "An Analysis of Supersonic Flow Phenomena in Conical Nozzles by a Method of Characteristics," NASA TN D-3550, Aug. 1966.

Cooling Effectiveness of Slot Injection into a Turbulent Boundary Layer

Floyd G. Howard* and Andrew J. Srokowski†
NASA Langley Research Center, Hampton, Va.

Introduction

SYSTEMS studies show that lighter and less expensive structural materials can be used if the operational surface temperature of a high-speed vehicle can be reduced below the normal radiation equilibrium temperature. Transpiration cooling, which is the process of releasing a coolant gas through a porous surface material, has been demonstrated as an effective means of maintaining the surface temperature below a required level.[1,2] However, a porous material may not be structurally practical for the skin of high-speed vehicles. A second technique for cooling a surface is to inject coolant gas through a discrete slot into the boundary layer. A considerable number of investigations[3-5] having been conducted on the cooling effectiveness ϵ for a single slot. Cooling effectiveness for high-speed flow usually is defined as

$$\epsilon = (T_t - T_{eq})/(T_t - T_j)$$

where T_t, T_{eq}, and T_j are the stream stagnation temperature,

Received May 31, 1977.

Index categories: Boundary Layers and Convective Heat Transfer – Turbulent; Jets, Wakes, and Viscid – Inviscid Flow Interactions; Supersonic and Hypersonic Flow.

*Aerospace Engineer, Fluid Mechanics Branch, High-Speed Aerodynamics Division.

†Aerospace Engineer, Fluid Mechanics Branch, High-Speed Aerodynamics Division. Member AIAA.

surface equilibrium temperature, and stagnation temperature of the injected coolant, respectively.

Numerical and experimental investigations[6-8] have shown that, for a given total mass of coolant, cooling performance can be enhanced by use of multiple slots. Some investigations have not determined ϵ directly from measured T_{eq} but have inferred the value of ϵ indirectly from heat-transfer measurements and estimated heat-transfer coefficients downstream of one or more slots. The experimental data shown in this Note were determined from direct temperature measurements of the surface at equilibrium conditions. The purpose of this Note is to show experimentally the influence on cooling effectiveness of injection through multiple flush slots at an angle of 10° into a turbulent Mach 6 boundary layer.

Experimental Approach

The tests were conducted in the Langley Mach 6 high Reynolds number tunnel.[9] This tunnel is a conventional blowdown type with an axisymmetric contoured nozzle followed by a 30.48-cm-i.d. pipe section, into which instrumented slot segments (schematically shown in Fig. 1) were installed at various axial stations. For present tests, the nominal operating conditions were total freestream pressure $\simeq 3.55 \times 10^6$ N/m²; $T_t \simeq 505$K; Mach number = 5.96; $0.53 \leq T_j/T_t \leq 0.68$; and $60 \times 10^6 \leq$ Reynolds number $\leq 110 \times 10^6$. Wall temperatures were measured on a thin wall liner located downstream of the last slot. Equilibrium surface temperatures T_{eq} were obtained by allowing the tunnel to run as much as 7.5 min; by this time the temperatures had leveled off at some constant value, taken as T_{eq}. The validity of this approach was substantiated by plotting surface temperature T_w against the reciprocal of time t and extrapolating to $1/t = 0$.

The slot mass flow ratio is defined as

$$\lambda = (\rho u)_j / (\rho u)_\infty$$

where ρ and u represent density and velocity, respectively, and subscripts j and ∞ denote slot exit and freestream conditions, respectively. The range of λ for this experiment was from approximately 0.05 to 0.28.

Air was injected from one to four slots into a turbulent Mach 6 boundary layer at an angle of 10° with the surface. The slots were spaced equally at 67.5 slot heights, with a slot height s of 0.254 cm.[10] The boundary-layer thickness at the location of the most upstream slot was approximately 4.32 cm.

Results

Slot injection reduces equilibrium surface temperature by modifying the boundary-layer temperature profile. Mixing between the coolant and the external boundary-layer flow begins immediately at the point of injection and continues downstream. The mixing between the slot flow and external boundary layer is most rapid for lower slot mass flow rates, and hence the downstream extent of high cooling effectiveness with the lower slot flow rates is reduced significantly.

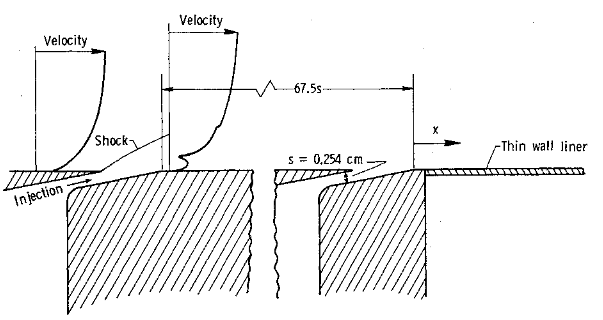

Fig. 1 Schematic of two-slot arrangement and thin wall liner.

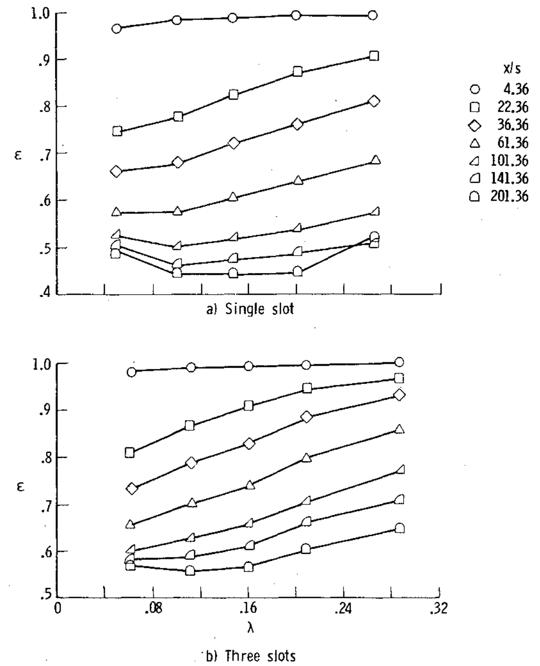

Fig. 2 Variation of experimental ϵ with λ.

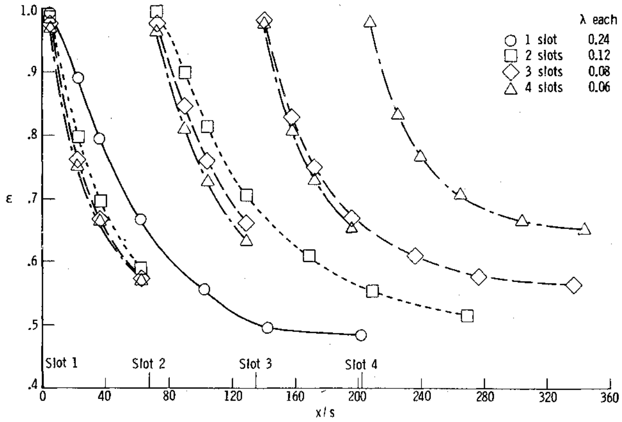

Fig. 3 Cooling effectiveness for one-, two-, three-, and four-slot case for the same total mass injection ($M_\infty = 5.96$, $T_j/T_t \simeq 0.6$).

Figure 2 shows examples of how experimental values of ϵ vary with λ downstream of a single slot and three slots. For x/s of 4.36, the level of ϵ for the single slot is approximately equal to the level of ϵ for three slots at all values of λ. At x/s of 22.36 and 36.36, ϵ for three slots is noticeably higher than for the single slot, but the improvement in ϵ as λ is increased ($d\epsilon/d\lambda$) is about the same. At the more downstream locations ($x/s > 60$), ϵ for three slots is only slightly higher than ϵ for the single slot at the lower values of λ. However, the increases in ϵ with increase in λ for three slots is significantly better than for the single slot.

Figure 3 shows experimental values of ϵ downstream of single- and multiple-slot (two, three, and four slots) injection, where the total mass injection (total mass = sum of flow rates from each slot) is the same for each case. The surface area maintained above a given ϵ increases as the number of slots is increased from one to four. For example, if operating conditions required $\epsilon \geq 0.5$, the single slot with λ of 0.24 would protect the surface to about 140 slot heights (x/s). Two, three, or four slots with the same total mass injection would protect the surface for a distance of more than 280, 340, and 400 slot heights, respectively. If an ϵ of 0.75 or 0.80 were required, the slot spacing would have to be closer than the present arrangement; with $\lambda = 0.06$ a second slot would be required at approximately x/s of 17 (see Fig. 3) to maintain $\epsilon \geq 0.8$. As illustrated by numerical studies,[7] the slot spacing can be increased with each additional slot because of the "multiple slot effect." However, it should be obvious that the slot spacing will reach some asymptotic value (although not shown in this Note).

In summary, the current study shows that, for a given coolant mass flow rate, thermal protection over the maximum surface area can be accomplished best by injecting the coolant flow through multiple slots. It is of interest to note that numerical studies[11] indicate that skin-friction reduction by slot injection is most effective when the total available mass is injected from one slot as far upstream as possible.

References

[1] Laganelli, A.L., "A Comparison between Film Cooling and Transpiration Cooling Systems in High-Speed Flow," AIAA Paper 70-153, New York, Jan. 1970.

[2] Eckert, E.R.G. and Livingood, J.N.B., "Comparison of Effectiveness of Convection, Transpiration, and Film Cooling Methods With Air as a Coolant," NACA TR-1182, 1953.

[3] Hefner, J.N., Cary A.M., Jr., and Bushnell, D.M., "Investigation of the Three-Dimensional Turbulent Flow Downstream of Swept Slot Injection in Hypersonic Flow," AIAA Paper 74-679, Boston, Mass, July 1974.

[4] Cary, A.M., Jr. and Hefner, J.N., "Film Coolir Effectiveness in Hypersonic Turbulent Flow," *AIAA Journal,* Vol. 8, Nov. 1970, pp. 2090-2091.

[5] Parthasarathy, K. and Zakkay, V., "An Experimental Investigation of Turbulent Slot Injection at Mach 6," *AIAA Journal,* Vol. 8, July 1970, pp. 1302-1307.

[6] Sherman, A., Yeh, H., Reshotko, E., and McAssey, E., Jr., "Multiple Slot Laminar Film Cooling," AIAA Paper 72-290, San Antonio, Texas, April 1972.

[7] Bushnell, D.M., "Hypersonic Airplane Aerodynamic Technology. Vehicle Technology for Civil Aviation – The Seventies and Beyond," NASA SP-292, 1971.

[8] Mark, A., "Film Cooling With Injection Through Multiple Slots," Ph.D. Thesis, Drexel Inst. of Technology, 1969.

[9] Jones, R.A. and Feller, W.V., "Preliminary Surveys of the Wall Boundary Layer in a Mach 6 Axisymmetric Tunnel," NASA TN D-5620, Feb. 1970.

[10] Srokowski, A.J., Howard, F.G., and Feller, W.V., "Direct Measurements at Mach 6 of Turbulent Skin-Friction Reduction by Injection from Single and Multiple Slots," AIAA Paper 76-178, Washington, D.C., Jan. 1976.

[11] Howard, F.G., Hefner, J.N., and Srokowski, A.J., "Multiple Slot Skin Friction Reduction," *Journal of Aircraft,* Vol. 12, Sept. 1975, pp. 753-754.

Turbulence Measurement in Transonic Flow

William C. Rose*
NASA Ames Research Center, Moffett Field, Calif.
and
Edward P. McDaid†
University of Washington, Seattle, Wash.

Nomenclature

a_w = overheat parameter $(R_w - R_r)/R_r$
d = wire diameter
E = wire voltage
h = film coefficient
k = thermal conductivity
K = $d \ln R_w / d \ln T_w$
l = wire length
m = $d \ln \mu / d \ln T$
M = Mach number
n = $d \ln k / d \ln T$
Nu = Nusselt number, hd/k
p = static pressure
R = resistance
R_s = series resistance in anemometer bridge
Re_δ = Reynolds number, $\rho u \delta / \mu$
Re_t = Reynolds number, $\rho_t u d / \mu_t$
S = sensor sensitivity coefficient
T = temperature
u = streamwise velocity
y = distance normal to wall
α = $1/\{1 + [(\gamma - 1)/2]M^2\}$
δ = boundary-layer thickness
η = recovery factor T_r/T_t
μ = viscosity
ρ = density
τ_{wr} = temperature overheat $(T_w - T_r)/T_r$
$\langle ( ) \rangle$ = root mean square

Superscripts

$( )'$ = fluctuating value
$\bar{( )}$ = time-averaged value

Subscripts

e = boundary-layer edge
r = recovery or adiabatic wall
t = total or stagnation conditions
u = velocity
w = wire
ρ = density
ρu = mass flux

Presented at the AIAA 9th Aerodynamic Testing Conference, Arlington, Texas, June 7-9, 1976 (in bound volume of papers); submitted Sept. 27, 1976; revision received April 28, 1977.

Index categories: Boundary Layers and Convective Heat Transfer – Turbulent; Subsonic Flow; Transonic Flow.

*Research Scientist. Member AIAA.

†Research Associate, Mechanical Engineering Department.

Introduction

TWO instrumentation systems are presently practical to use in turbulence measurements. The laser velocimeter can measure fluctuating velocities while the hot-wire anemometer, in principle, can be used to obtain both kinematic and thermodynamic fluctuations. In addition, the hot-wire anemometer gives an analog signal output that is convenient for use in time-space correlation studies and spectral analyses. The laser velocimeter has been used successfully in all flow regimes, whereas the hot wire has not been exploited in transonic flows, where the wire response has not been well-understood. The purpose of this Note is to examine the response and calibration of a constant-temperature, hot-wire anemometer in transonic flow and to present turbulence measurements, obtained in a transonic boundary layer.

Results and Discussion

Any discussion of hot-wire anemometry in transonic flow must consider past work in that area of interest. In particular, one must consider how little activity has existed in an area of such technological importance. Twenty years have passed since the significant work of Morkovin[1] was published which outlined the basic considerations of hot-wire anemometry applied to transonic flows. Although Morkovin indicated the possible difficulty in interpreting the wire's signal, he certainly did not indicate that it was impossible to do so.

In order to recount the problems discussed by Morkovin, the following discussion is presented. The nomenclature used, and the ideas considered, follow those of Morkovin and Kovasznay[2] closely. Generally, the fluctuating voltage given by a hot-wire anemometer E' referenced to its mean value $\bar{E}$ is a function of the heat transfer from the wire, which is directly