DOWNSTREAM INFLUENCE OF SWEPT SLOT INJECTION IN HYPERSONIC TURBULENT FLOW

Jerry N. Hefner, Aubrey M. Cary, Jr., and Dennis M. Bushnell

Langley Research Center
Hampton, Va. 23665
Results of an experimental and numerical investigation of tangential swept slot injection (sweep angles of 22.5° and 45°) into a thick turbulent boundary layer at Mach 6 are presented. Film cooling effectiveness, skin friction, and flow structure downstream of the swept slot injection are investigated. The data are compared with that for unswept slots, and it is found that cooling effectiveness and skin-friction reductions are not significantly affected by sweeping the slot. Predictions of cooling effectiveness and skin friction obtained by a numerical finite-difference technique agree reasonably well with experimental surface variables.
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SUMMARY

Results of an experimental and numerical investigation of tangential swept slot injection (sweep angles of 22.5° and 45°) into a thick turbulent boundary layer at Mach 6 are presented. Film cooling effectiveness, skin friction, and flow structure downstream of the swept slot injection are investigated. The data are compared with that for unswept slots, and it is found that cooling effectiveness and skin-friction reductions are not significantly affected by sweeping the slot. Predictions of cooling effectiveness and skin friction obtained by a numerical finite-difference technique agree reasonably well with experimental surface variables.

INTRODUCTION

Surface temperatures of an uncooled hypersonic aircraft may reach very high temperatures and may dictate the use of hot structural concepts. Systems studies show that lighter and less expensive structural materials can be used if these operational surface temperatures can be reduced below the radiation equilibrium temperature (ref. 1). Film cooling provides one means of reducing these high operational surface temperatures. Experimental studies of tangential slot injection at Mach 6 (refs. 2 and 3) have shown that the film cooling in two-dimensional, high-speed turbulent flow is significantly more effective than that indicated by extrapolations of low-speed results to high-speed conditions. However, many practical applications of slot injection film cooling (particularly on wings) will probably require the slots to be swept relative to the surface streamline direction. Reference 4 presents heat-transfer results which indicate the combined effects of three-dimensionality and pressure gradient on film cooling on a cone at angle of attack. However, detailed studies have not been conducted to determine the effect of sweeping the slot relative to the inviscid streamlines on the film cooling effectiveness. Furthermore, it is important to determine whether the skin-friction reductions found for two-dimensional slot injection (ref. 5) could be realized for the three-dimensional turbulent flow downstream of swept slot injection. Therefore, the present investigation was conducted to determine both numerically and experimentally the effect of sweep on film cooling effectiveness, skin friction, and flow structure downstream of the slot.

The experimental data obtained in this investigation are important for evaluating slot injection film cooling as an active cooling technique. If these data indicate that three-dimensional slot injection film cooling is as effective
as the two-dimensional case, then systems studies would be appropriate to ascer­
tain the merits of slot-cooled aircraft structures. In addition, these data
provide a unique test case (that is, near zero pressure gradient and zero cur­
vature external flow) for validating numerical three-dimensional turbulent
boundary-layer solutions currently under development. The numerical and exper­
imental information generated is also necessary for assessing other possible
uses for slot injection such as inlet boundary-layer control (ref. 6), aero­
dynamic windows for gas dynamic lasers (ref. 7), and low-speed drag-reduction
systems.

The present paper reports measurements at Mach 6 of surface equilibrium
temperature (cooling effectiveness), skin friction, and boundary-layer profiles
(pitot pressure, total temperature, and flow angle) downstream of two swept
slots ($\Lambda = 22.5^\circ$ and $\Lambda = 45^\circ$). The experiments utilized sonic, tangential
air injection into a thick turbulent boundary layer. Surface pressures and oil­
flow data were obtained to aid in interpreting the flow structure downstream of
the slot. The effects of slot height and injectant temperature on the swept
slot cooling effectiveness are measured and discussed. The cooling effective­
ness and skin friction as well as boundary-layer profiles downstream of the
swept slots are compared with previous data for unswept slots. A finite­
difference solution developed for unswept tangential slot injection (ref. 8)
was modified to account for swept slot injection, and predictions obtained by
this technique are compared with the experimental data.

SYMBOLS

A2 mixing-length ratio in mixing zone (see fig. 4(a) and ref. 8)
Cf skin-friction coefficient, $\tau_w/q$
D diameter
I electrical current
M Mach number
p pressure
q dynamic pressure, $\frac{1}{2}\rho u^2$
R wire resistance
S slot height
T temperature
t slot lip thickness (0.159 cm)
u velocity in free-stream direction
\( u_N \) velocity perpendicular to slot
\( x \) distance downstream of slot parallel with free stream
\( y \) distance above surface, downstream of slot
\( y' \) distance above top of slot lip, \( y - S - t \)
\( \delta \) boundary-layer velocity thickness (at \( u/u_\infty \approx 0.995 \))
\( \epsilon \) cooling effectiveness parameter, \( \frac{T_{t,\infty} - T_{eq}}{T_{t,\infty} - T_{t,j}} \)
\( \Theta \) surface streamline direction measured from streamline parallel to free-stream direction in plane of flat plate (see fig. 1)
\( \Lambda \) slot sweep angle in plane of flat plate (see fig. 1)
\( \lambda \) mass flow parameter, \( \frac{\rho j u_N, j}{\rho_\infty u_\infty} \)
\( \mu \) viscosity
\( \rho \) density
\( \tau_w \) component of shear stress in free-stream direction
\( \phi \) wire output function (see fig. 3)

Subscripts:
\( \text{eq} \) equilibrium
\( j \) slot
\( o \) without slot
\( p \) probe
\( t \) total or stagnation
\( w \) wall
\( w_1, w_2 \) wire 1 and wire 2, respectively
\( \infty \) free stream
DESCRIPTION OF INVESTIGATION

Experimental Investigation

Facility and model.—The experimental investigation was conducted in the Langley 20-inch Mach 6 tunnel (see appendix of ref. 9 for a description of the tunnel) at a free-stream total temperature and unit Reynolds number per centimeter of 492 K and 0.287 \( \times 10^6 \), respectively. The model (see fig. 1) was an epoxy resin fiberglass flat plate (35.5 cm wide and 91.4 cm long) with end plates and was mounted parallel with and recessed below the flat tunnel wall. The slot flow was injected tangentially over the surface of the flat plate from a two-dimensional sonic slot swept at either 22.5° or 45° relative to the free-stream flow direction. The slot flow was injected normal to the slot face and the local flow angle at the slot was therefore 22.5° or 45° relative to the free-stream flow. An example of the surface flow development downstream of the 45° swept slot is shown in figure 2 where the sketched streamlines were obtained from experimental surface oil-flow studies. The surface flow is normal to the slot face near the slot and gradually turns parallel with the free-stream direction further downstream. The slot configuration was identical to that used in references 3 and 5 and could be adjusted to provide slot heights \( S \) of 0.159, 0.476, and 1.111 cm and the lip thickness \( t \) was held constant at 0.159 cm. The slot mass flow rate \( \rho_j u_N j \), found to be uniform over a midspan of at least 20 cm in reference 5, was varied to insure that the velocity at the slot exit was sonic. The ratio of measured slot mass flow rate to calculated free-stream mass flow rate \( \left( \lambda = \rho_j u_N j / \rho_{\infty} u_{\infty} \right) \) ranged from 0.042 to 0.838. The temperature of the slot flow was controlled by using a liquid nitrogen heat exchanger and for this investigation was varied from 136 K to 311 K.

Surface measurements.—Plate surface temperatures were measured along the center line by flush-mounted thermocouples and skin friction was measured by floating element gages located 6.36 cm off center line. Pressure orifices were located at the slot exit and nominally 3.81 cm off center line at several downstream x-locations.

A plate surface temperature was considered to be in equilibrium when it changed less than 0.1 percent over a time interval of 100 seconds. This criterion, although arbitrary, should provide sufficiently accurate values of equilibrium surface temperature. Generally, only the forward portion of the plate surface \( (x < 65 \text{ cm}) \) reached equilibrium temperature in the available test time \( (\approx 1800 \text{ sec}) \).

The null-type skin-friction balances used in this study employed a floating element with a diameter of 0.94 cm and a surrounding gap width of 0.01 cm; the axes of the balances were aligned with the free-stream flow. The balances had multiple sensitivities of 0.15 g/cm², 1.5 g/cm², and 15 g/cm² and were statically calibrated before and after the test program by applying a known load and measuring the voltage necessary to null the balance. The calibration remained constant throughout the tests within 3 percent of the measured value. Water-cooled jackets were used to maintain the balance internal temperature below 360 K at all times; thus, experimental errors due to temperature sensitivity of the balance coil were significantly reduced.
A mixture of silicon oil and lampblack was distributed in random dots of various sizes over the surface of the model to study the surface streamline patterns. Motion pictures (35 mm) were taken of the oil-flow patterns during the tests. Surface flow angles were measured graphically from the oil-flow patterns photographed during the run. The surface streamlines indicated that the flow exited normal to the slot as shown in figure 2.

Probe measurements.- Surface and pitot pressures were measured by multi-range capacitance-type pressure transducers calibrated to better than 1-percent full-scale accuracy on each range. The tip of the pitot probe was 0.046 cm high and 0.198 cm wide; the total temperature probe was a shielded thermocouple with base bleed and a circular tip of 0.191 cm diameter. The temperature probe was calibrated for the conditions of the present tests and was found to have a recovery factor of 0.99. Based on the extensive calibrations of a similar probe over a wider range of Reynolds number and Mach number (ref. 5), this recovery factor was assumed to be valid for the present study and was used to correct the total temperature probe data. Velocities were calculated by using the corrected probe values of temperature and the measured pitot pressure along with the assumption of constant static pressure across the boundary layer.

Flow angle measurements.- Boundary-layer flow angle measurements were obtained 5.40 cm downstream of the 22.5° swept slot and 8.26 cm downstream of the 45° swept slot by using a swept, dual hot-wire probe (fig. 3) similar to that used in reference 10. The hot-wire probe was constructed from two small spirals of platinum-iridium wire, each stretched across and silver-soldered to notched needles. Platinum-iridium wire was used instead of the more conventional copper-plated tungsten wire because of the high temperatures and oxidation problems of the present tests. Also the wire was stretched into a loose spiral rather than a tightly wound coil and provided the necessary strength for the rather high dynamic pressures of the Mach 6 wind-tunnel facility. The wire diameter was 0.0025 cm and the ratio of wire length to wire diameter was 110. The probe was calibrated in the Mach 6 free stream by setting the probe at known yaw angles and measuring the probe output. (See calibration curve in fig. 3(b).) The probe was calibrated so that the response was essentially independent of local Reynolds number and Mach number.

Numerical Investigation

A numerical finite-difference solution procedure for tangential slot injection into supersonic turbulent boundary layers (ref. 8) was modified to account for swept slot injection by including the spanwise momentum equation in the solution. (See refs. 11 and 12 for a description of the swept slot code without the slot injection eddy viscosity option, which was modeled after ref. 8.) The present calculation is therefore essentially a combination of the capabilities existing in references 8, 11, and 12. The three-dimensional turbulence model used was the "invariant turbulence" or scalar eddy viscosity approach discussed in references 11 and 12. This approach uses the usual boundary-layer approximation of constant static pressure in the direction normal to the surface.
RESULTS AND DISCUSSION

Flow Structure and Surface Pressure Distribution

The slot flow field is schematically shown in figure 4(a) for the case of nearly "matched" pressure conditions. Ideally, the "matched" pressure condition refers to the case where the slot and free-stream static pressure are equal and the static pressure along the wall is nearly constant \( P_w/P_{w,o} \approx 1 \). As shown in figure 4(b), this condition cannot be fully realized in the region very close to the slot exit \( x/S \approx 0.6 \) unless the slot flow is allowed to expand fully; the fully expanded slot flow would match the static pressure in the immediate vicinity of the slot with the free-stream static pressure, but would generate pressures lower than the stream static pressure further downstream of the slot (ref. 5). Figure 4(b) shows that as the mass flow rate \( \lambda \) was increased from the no-injection case, the wall static pressure in the slot region increased until the slot flow became sonic; at this point the slot pressure was slightly less than the stream static pressure \( P_{w,o} \) because of the expansion of the free-stream flow over the slot lip. (See fig. 4(a).) Further increasing the slot mass flow rates first reduced the near slot pressures and then increased them. This subsequent increase in the near slot pressure is caused by the slot fully expanding; the Mach number distribution in the near slot region remains constant. Since the present investigation is primarily concerned with the case where the slot flow is sonic and the downstream pressure gradient is as small as possible, most of the slot mass flow rates are at near "matched" pressure conditions (that is, where the slot flow just becomes sonic and the near slot pressures are somewhat less than the stream static pressure).

Surface static pressures measured at seven streamwise locations downstream of the 22.5° swept slot and eight streamwise locations downstream of the 45° swept slot are shown in figures 4(c) and 4(d), respectively, for the three slot heights at near "matched" pressure conditions. The wall static pressure \( P_w \) is normalized by the measured stream static pressure with no slot \( P_{w,o} \). Although an adverse pressure gradient exists close to the slot exit, the surface pressures further downstream of the slot are very near free-stream static pressure in all cases.

Cooling Effectiveness

A summary of the equilibrium temperatures for near "matched" pressures downstream of the swept slots (\( A = 22.5° \) and 45°) for \( T_{t,j}/T_{t,\infty} = 0.6 \) and 0.32 is presented in figures 5 and 6 in a form which correlated effectiveness data in references 2, 3, and 5. The distance parameter \( (x/S)\lambda^{-0.8} \), which is based on the chordwise distance downstream of the slot (parallel with the mainstream flow direction) correlates the effectiveness data for both slot flow temperatures in a relatively narrow band for the slot mass flow rates and slot heights of the present investigation. Note that the data appear to correlate at a given slot flow total temperature, but the correlation is different for the two different slot temperature levels. The data appear to correlate as long as \( \lambda \) is changed by changing the ratios of slot density to free-stream density; if the ratio of slot velocity to the free-stream velocity is changed, the correlation also changes. (Compare figs. 5 and 6.) Table I presents coefficients
for the straight-line correlations of the swept slot data shown in figures 5 and 6 as well as those for the unswept slot data of reference 5 for similar total temperature ratios and mass flow rates.

The effect of slot sweep on the film cooling effectiveness is clearly seen in figures 5 and 6. Although the cooling effectiveness is lower and decreases more rapidly for the cooler slot flow temperature ($T_{t,j}/T_{t,\infty} \approx 0.32$), the film cooling effectiveness for the swept slots decreases only slightly below that for the comparable unswept slots for both sweep angles and both slot flow temperatures. However, theoretically the total cooled area downstream of the swept slot is reduced by cosine $\Lambda$ when compared with the unswept slot; the oil-flow studies tend to confirm this condition. (For example, fig. 2 shows that the streamlines downstream of the swept slot remain parallel as the slot flow mixes with the stream boundary layer and turns toward the free-stream direction.) As was found in reference 5 for the unswept slots, the cooling effectiveness for the swept slots for $(x/S)\lambda \approx 0.8 < 100$ is less than the expected value of $\varepsilon = 1$ because of heat conduction from the free-stream flow through the metal slot lip and into the slot flow.

Cooling effectiveness data for mass flow rates greater than the near-matched pressure condition are shown in figure 7 for the 22.5° swept slot with $T_{t,j}/T_{t,\infty} \approx 0.6$ and 0.45. The straight-line correlations of the data shown in figures 5 and 6 and table I are shown for comparison with the data for the more fully expanded slot flows. The higher mass flow rates slightly increase the effective cooling lengths (that is, the distance downstream of slot where $\varepsilon \approx 1.0$) for the 22.5° swept slot; however, the rate of decrease in effectiveness beyond this cooling length is approximately the same as that found for near-matched pressure conditions. Since sweeping the slot has only a minor influence on effectiveness ($\Lambda \leq 45^\circ$), the favorable effects of slot injection in high-speed flow are retained. (See refs. 3 and 5.)

Skin Friction

Local surface skin-friction measurements presented in terms of $C_f/C_{f,0}$ are shown for $T_{t,j}/T_{t,\infty} \approx 0.6$ in figure 8 for both sweep angles and the three slot heights. Skin-friction data downstream of the unswept slot (ref. 5) at similar test conditions are shown in the figure for comparison. Skin-friction data for the swept slots at $T_{t,j}/T_{t,\infty} \approx 0.32$ were not obtained in the present study since reference 5 showed that decreasing the slot flow temperature had a negligible effect on the skin friction even though the cooling effectiveness was significantly reduced. Sweeping the slot reduces the measured skin friction downstream of the slot below that for the unswept two-dimensional slot for the three slot heights investigated; therefore, the significant reductions in skin friction found for the unswept slot (ref. 5) were also found for the swept slots.

Comparison of Finite-Difference Predictions With Experiment

Surface parameters.- The implicit finite-difference solution procedure developed for tangential slot injection into supersonic turbulent boundary layers (ref. 8) was modified to predict the flow downstream of swept slot
injection. Initial slot and boundary-layer profiles normal to the slot face were required as inputs into the numerical technique and were obtained experimentally for the largest slot height \(S = 1.111 \text{ cm}\) for both the 22.5\(^\circ\) and 45\(^\circ\) swept slots (shown by the solid curves in fig. 9). Initial slot profiles were measured for both \(T_{t,j}/T_{t,\infty} \approx 0.6\) and 0.32 for the 22.5\(^\circ\) swept slot, whereas only the profiles for \(T_{t,j}/T_{t,\infty} \approx 0.6\) were measured for the 45\(^\circ\) swept slot. The slot velocity profiles obtained from measurements near the lower slot wall were modified for some calculations in an attempt to ascertain the effect of a "laminar-like" slot velocity profile on the downstream flow predictions; the measured slot velocity profiles near the lower wall are subject to interpretation because of possible probe interference effects. Also a laminar slot flow boundary layer may have occurred since the slot Reynolds number \(\rho j U_N j S/\mu_j\) was near transitional (approximately \(3 \times 10^3\)) and the slot flow was rapidly accelerated just prior to being injected (ref. 5). The stream or external boundary-layer profile shapes are typical of nozzle wall turbulent boundary layers, and the velocity thickness \(\delta\) was approximately 4.5 cm. (See table II for a tabulation of the measured nozzle wall profiles.) The calculation was carried out in a coordinate system normal to the slot face. The external spanwise flow (assumed to be invariant in planes parallel to the slot face) was input from a vector decomposition of the measured tunnel wall boundary-layer profile.

As was discussed previously, a pressure gradient existed near the slot even for the nearly "matched" pressure condition. The effect of this gradient was explored numerically; the most "severe" pressure gradients that could be input into the swept slot program without causing flow separation are shown as unbroken fairings in figure 10 for the swept slots. It is readily seen that the maximum \(dp/dx\) used in the calculation is much less than local values indicated by the experimental data. (See section on "Flow Structure and Surface Pressure Distribution" for an explanation of these pressure gradients.) Therefore, based upon these calculations and careful study of the oil flow near the slot, it is tentatively concluded that the low velocity flow from the slot is locally separated for this slot height. However, the influence of this separation on surface shear and temperature seems to quickly "wash out" downstream. Limited numerical predictions were also obtained with a reduced mixing-length ratio \(A_2\) for the free mixing region downstream of the slot lip. (See ref. 8 for a detailed discussion of the role of \(A_2\) in the eddy viscosity model.) The reason for varying this parameter will be discussed in connection with the downstream profile comparisons.

Cooling effectiveness and skin-friction predictions are compared with the experimental results in figures 11 and 12, respectively. The laminar slot predictions with zero pressure gradient and the turbulent slot predictions both with and without the adverse pressure gradient for a mixing-length ratio \(A_2\) of 0.09 predict reasonably well the measured effectiveness and skin-friction data for the 22.5\(^\circ\) swept slot for \(T_{t,j}/T_{t,\infty} \approx 0.6\). The laminar slot predictions for the 22.5\(^\circ\) swept slot with the adverse pressure gradient were not obtained since the prediction technique indicated that the input pressure gradient was too severe for the laminar slot flow and with even the moderate \(dp/dx\) shown in figure 10 forced the slot flow to separate locally. Decreasing the mixing-length ratio for the free mixing region from 0.09 to 0.06 for the laminar slot without pressure gradient adversely affects the agreement between
the predicted and measured cooling effectiveness but enhanced the agreement between the predicted and measured skin friction downstream of the slot. (See fig. 12(a) for $x/S > 10$.) For the 45° swept slot, only the predictions for the laminar slot flow with pressure gradient and with a reduced mixing-length ratio ($A_2 = 0.06$) gave reasonable agreement with the cooling effectiveness and skin-friction data. Limited attempts to predict the cooling effectiveness data for the cooler slot flow temperature ($T_{t,j}/T_{t,\infty} = 0.32$) for the 22.5° swept slot (fig. 11(c)) indicated fair agreement between the predictions and the data. The comparison of the predicted and measured effectiveness for the warmer slot flow ($T_{t,j}/T_{t,\infty} = 0.6$) appeared to indicate that the pressure gradient did not significantly affect the downstream calculations; therefore, the cooler slot flow calculations were conducted only for a zero pressure gradient.

Profile and flow angle comparisons.- Although the finite-difference calculations investigated do provide reasonable predictions of the trends and approximate magnitudes of the measured cooling effectiveness and skin-friction data, these same calculations fail to predict the velocity and total temperature profiles near the plate wall ($y < 2$ cm) at downstream $x$-locations where the slot flow has turned parallel with the stream direction. (See figs. 13 and 14.) Also, predictions of the flow angle through the viscous region are very poor compared with the experimental data. (See fig. 15.) The measured velocity profiles presented in figure 13 and table III for both swept slots show a concave velocity profile near the plate wall. This is not a three-dimensional effect since velocity profiles recently obtained in the present investigation downstream of the two-dimensional slot configuration of references 3 and 5 (fig. 13(a) and tables III(a) and III(b)) are similarly shaped. Furthermore, this anomaly was also found in the two-dimensional slot injection studies of references 13 and 14. Reducing the mixing-length ratio for the free mixing length region downstream of the slot proved to be partially effective in achieving agreement between predicted velocity profiles and measured profiles in references 13 and 14. In the present investigation, reducing the mixing-length ratio did have a favorable influence on the prediction of the measured velocity profiles, but made the already poor predictions of the measured surface flow angles (from oil flow) and flow angle profiles (from swept hot-wire probe) even worse. (See figs. 15 and 16.) It is believed that one plausible reason that the surface flow turns more rapidly than predicted is that the slot flow separates locally in the region very near the slot exit. Figure 16 shows that the predicted rate of surface flow turning (the change in $\theta$ with respect to $x$) downstream of the region where the slot flow remains perpendicular to the slot appears to agree reasonably well with $d\theta/dx$ from the experimental results.

The comparison of the present swept and unswept slot finite-difference predictions with experimental profiles (figs. 13, 14, and 15) and similar comparisons for two-dimensional slot injections in reference 14 show that the present understanding of the turbulent mixing which occurs for slot injection into a thick supersonic/hypersonic turbulent boundary layer is insufficient. The present calculations tend to suggest that (a) the input pressure gradient is only important near the slot and tends to disappear 20 to 40 slot heights downstream and (b) the concave velocity profiles indicate either a "laminar-like" mixing region or the influence of the adverse experimental pressure gradient which was considerably larger than could be included in a "boundary-layer"
method. (See fig. 10.) However, for the two-dimensional slot flow case, recent improved turbulence modeling research, reported in reference 15, indicates very good agreement with the $M_\infty = 6$ profiles shown in figures 13(a) and 14(a).

CONCLUSIONS

The present data, consisting of (1) adiabatic wall temperature distributions, (2) direct skin-friction measurements, (3) surface flow angles, (4) three-dimensional turbulent flow profiles, and (5) surface pressure distributions over a range of sweep angle, slot height, slot mass flow rate, and slot total temperature comprise a fairly complete and definitive study of the influence of three-dimensionality upon tangential slot injection. In addition, the paper presents results of a new three-dimensional calculation method for slot injection. Major conclusions resulting from the present experimental and numerical investigation are as follows:

1. The large film cooling effectiveness found for unswept slots at supersonic/hypersonic speeds was not significantly affected by sweeping the slot to 22.5° and 45° for either $T_{t,j}/T_{t,\infty} \approx 0.6$ or 0.32. In agreement with the two-dimensional case, the film cooling effectiveness downstream of the swept slots decreased when the ratio of the slot to free-stream total temperature $T_{t,j}/T_{t,\infty}$ was reduced from 0.6 to 0.32. For the range of slot heights investigated, slot height did not significantly affect the swept slot cooling effectiveness correlation.

2. The large reductions in local surface skin friction measured downstream of unswept slots still occurred as the sweep angle was increased for all slot heights investigated.

3. Predictions of the film cooling effectiveness and skin friction obtained by a numerical technique agree reasonably well with the measured results for both sweep angles.

4. The swept slot finite-difference solution with the simple "invariant turbulence" or scalar eddy viscosity model fails to predict the downstream velocity, total temperature, and flow angle profiles in the inner or slot mixing region. This disagreement is tentatively ascribed to the existence of a localized three-dimensional separation of the slot flow immediately downstream of the slot exit which allows the near wall flow to turn prematurely under the action of pressure forces.

Langley Research Center
National Aeronautics and Space Administration
Hampton, VA 23665
June 2, 1977
REFERENCES


TABLE I.- COOLING EFFECTIVENESS CORRELATIONS

FOR SWEPT SLOT INJECTION

\[ \varepsilon = a \left[ \frac{x}{S} \lambda^{-0.8} \right]^{-b} \]

<table>
<thead>
<tr>
<th>( \Lambda ), deg</th>
<th>( \frac{T_{t,j}}{T_{t,\infty}} )</th>
<th>( a )</th>
<th>( b )</th>
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<td>0</td>
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<td>45</td>
<td>0.32</td>
<td>5.19</td>
<td>0.393</td>
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TABLE II.- TABULATION OF MEASURED BOUNDARY-LAYER PROFILE AT SLOT

\[ P_{t, \infty} = 3.50 \text{ MPa; } T_{w}/T_{t, \infty} = 0.73 \]

<table>
<thead>
<tr>
<th>( y', \text{ cm} )</th>
<th>( P_{t, p'} \text{, kPa} )</th>
<th>( T_{t, p'} \text{, K} )</th>
<th>( M/M_{\infty} )</th>
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# TABLE III. - TABULATION OF MEASURED BOUNDARY-LAYER PROFILES

(a) $\Lambda = 0^\circ$; $T_{t,j}/T_{t,\infty} = 0.6$; $\lambda = 0.047$;
$S = 1.111$ cm; $x = 10.8$ cm; $M_\infty = 6.05$;
$T_w/T_{t,\infty} = 0.64$; $p_{t,\infty} = 3.57$ MPa;
$T_{t,\infty} = 482.7$ K

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<th>$T_{t,j}$, K</th>
<th>$M/M_\infty$</th>
<th>$u/u_\infty$</th>
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TABLE III. - Continued

(b) \( \Lambda = 0^\circ; \ T_{t,1}/T_{t,\infty} = 0.6; \ \lambda = 0.047; \ S = 1.111 \text{ cm}; \ x = 27.6 \text{ cm}; \ M = 6.04; \ T_w/T_{t,\infty} = 0.67; \ p_{t,\infty} = 3.56 \text{ MPa}; \ T_{t,\infty} = 486.1 \text{ K} \)

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TABLE III.—Continued

(c) \( \Lambda = 22.5^\circ; \ T_t,j/T_t,\infty = 0.6; \ \lambda = 0.042;\)
\(S = 1.111 \text{ cm}; \ x = 21 \text{ cm}; \ M_\infty = 6.01;\)
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\(T_{t,\infty} = 495 \text{ K}\)

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TABLE III.- Continued

(d) \( \Lambda = 45^\circ; \ T_t/j/T_{t,\infty} = 0.6; \ \lambda = 0.048; \)
\( S = 1.111 \text{ cm}; \ x = 23 \text{ cm}; \ M_{\infty} = 6.0; \)
\( T_w/T_{t,\infty} = 0.66; \ p_{t,\infty} = 3.56 \text{ MPa}; \)
\( T_{t,\infty} = 498 \text{ K} \)

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TABLE III.—Concluded

(e) \( A = 22.5; \ T_{t,j}/T_{t,\infty} = 0.32; \ \lambda = 0.061; \)
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\( p_{t,\infty} = 3.57 \text{ MPa}; \ T_{t,\infty} = 491.7 \text{ K} \)

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(a) Schematic of model.

Figure 1.- Sketch of experimental three-dimensional film cooling model. Dimensions are in cm.
(b) Instrumentation layout.

Figure 1.- Concluded.
Figure 2.- Typical three-dimensional surface streamlines obtained from oil flow in instrumented region of 45° swept slot model.
Figure 3.- Schematic and calibration of dual, swept, hot-wire flow direction probe.
Rapidly slot
(a) Flow field downstream of slot.

Figure 4.- Flow field and pressure distribution downstream of swept slot.
(b) Effect of mass flow rate on surface pressure near slot. $x/S \approx 0.6$.

Figure 4.- Continued.
(c) Pressure distribution downstream of $\Lambda = 22.5^\circ$ slot.

Figure 4.—Continued.
(d) Pressure distribution downstream of $\Lambda = 45^\circ$ slot.

Figure 4.- Concluded.
Figure 5.- Film cooling effectiveness for $T_{t,j}/T_{t,\infty} \approx 0.6$ and near "matched" pressure conditions.
Figure 6.- Film cooling effectiveness for $T_{t,j}/T_{t,\infty} \approx 0.32$ and near "matched" pressure conditions.
Figure 7.- Cooling effectiveness for mass flow rates greater than "matched" pressure conditions. $\Lambda = 22.5^\circ$. 

**CORRELATION OF DATA, $\Lambda = 22.5^\circ$, AND $T_{t,j}/T_{t,\infty} = 0.6$**

(a) $T_{t,j}/T_{t,\infty} = 0.6$

- $S$, cm $\lambda$
  - $0.159$ $0.386$
  - $0.159$ $0.838$
  - $0.476$ $0.200$
  - $1.111$ $0.102$

(b) $T_{t,j}/T_{t,\infty} = 0.45$

- $S$, cm $\lambda$
  - $0.476$ $0.254$
  - $1.111$ $0.120$

$(x/ S)^{-0.8}$
Figure 8.- Skin friction downstream of slot for "matched" pressure conditions.

\[ 0.042 \leq \lambda \leq 0.077. \]
Figure 9.- Initial boundary-layer and slot profiles (normal to slot) used in the finite-difference calculations. $S = 1.111 \text{ cm.}$
\( \lambda = 0.048 \)

(b) \( \Lambda = 45^\circ \); \( T_t, j/T_{t,\infty} = 0.6 \).

Figure 9.– Continued.
Figure 9.- Concluded.

\[
\lambda = 0.061
\]

\[
\frac{u_N}{u_\infty \cos \Lambda}, \frac{T_t}{T_{t,\infty}}
\]

(c) \(\Lambda = 22.5^\circ\); \(T_t, j/T_{t,\infty} = 0.32\).
Figure 10.—Input pressure gradients for finite-difference program.

\[ \frac{p_w}{p_{w,0}} = 1.0 \]

\[ \Lambda = 22.5^\circ; \lambda = 0.042 \]

\[ \Lambda = 45^\circ; \lambda = 0.048 \]

\[ x, \text{ cm} \]

\[ T_{t,j}/T_{t,\infty} = 0.6; \; S = 1.111 \text{ cm}. \]
Experimental data
Lam. slot, zero press. grad., $A_2 = 0.09$
Lam. slot, zero press. grad., $A_2 = 0.06$
Lam. slot, press. grad., $A_2 = 0.06$
Turb. slot, zero press. grad., $A_2 = 0.09$
Turb. slot, press. grad., $A_2 = 0.09$

Figure 11.- Comparison of finite-difference predictions with experimental cooling-effectiveness data. $S = 1.111$ cm.

(a) $\Lambda = 22.5^0; \frac{T_{t,j}}{T_{t,\infty}} = 0.6; \lambda = 0.042$.

(b) $\Lambda = 45^0; \frac{T_{t,j}}{T_{t,\infty}} = 0.6; \lambda = 0.048$.

(c) $\Lambda = 22.5^0; \frac{T_{t,j}}{T_{t,\infty}} = 0.32; \lambda = 0.061$. 

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Figure 12. - Comparison of finite-difference predictions with experimental skin-friction data. \( S = 1.111 \) cm.

(a) \( \Lambda = 22.5^\circ; \; T_{t,j}/T_{t,\infty} = 0.6; \; \lambda = 0.042. \)
Figure 12. Concluded.

EXPRESSIMENTAL DATA

- LAM. SLOT, ZERO PRESS. GRAD., $A_2 = 0.09$
- LAM. SLOT, PRESS. GRAD., $A_2 = 0.09$
- LAM. SLOT, PRESS. GRAD., $A_2 = 0.06$
- TURB. SLOT, ZERO PRESS. GRAD., $A_2 = 0.09$
- TURB. SLOT, PRESS. GRAD., $A_2 = 0.09$

CURVES REPRESENT SWEPT SLOT PREDICTIONS

(b) $\Lambda = 45^\circ; \ T_{t,j}/T_{t,\infty} = 0.6; \ \lambda = 0.048$. 

Figure 12.- Concluded.
Predictions for turbulent slot, zero pressure gradient, $A_2 = 0.09$

(a) $\Lambda = 0^\circ$; $T_t, j/T_{t,\infty} = 0.6$; $\lambda = 0.047$.

Figure 13.- Comparison of finite-difference predictions with experimental velocity profiles. $S = 1.111$ cm.
Figure 13.—Continued.

(b) \( \Lambda = 22.5^\circ; \ T_t, j/T_{t,\infty} = 0.6; \ \lambda = 0.042; \ x = 21.1 \text{ cm}. \)
Figure 13.- Continued.

\( \Lambda = 45^\circ; \ T_{t,j}/T_{t,\infty} = 0.6; \ \lambda = 0.048; \ x = 23 \text{ cm.} \)
(d) $\Lambda = 22.5^\circ$; $T_{t,j}/T_{t,\infty} = 0.32$; $\lambda = 0.061$; $x = 21.1$ cm.

Figure 13.- Concluded.
Data, $x = 10.8$ cm
○ Data, $x = 27.6$ cm
--- Prediction, $x = 10.8$ cm
稳步 Prediction, $x = 27.6$ cm

Predictions for turbulent slot, zero pressure gradient, $A_2 = 0.09$

Figure 14. — Comparison of finite-difference predictions with experimental total temperature profiles. $S = 1.111$ cm.
(b) $\Lambda = 22.5^\circ$; $T_{t,j}/T_{t,\infty} = 0.6$; $\lambda = 0.042$; $x = 21.1$ cm.

Figure 14.- Continued.
Figure 14. Continued.

(c) $\Lambda = 45^\circ$; $T_{t,j}/T_{t,\infty} = 0.6$; $\Lambda = 0.048$; $x = 23$ cm.

Figure 14. Continued.
Figure 14.— Concluded.

(d) $\Lambda = 22.5^\circ; \frac{T_{t,j}}{T_{t,\infty}} = 0.32; \lambda = 0.061; x = 21.1$ cm.
Figure 15.- Comparison of finite-difference predictions with swept-wire flow angularity measurements. \( S = 1.111 \text{ cm}. \)
CURVES REPRESENT SWEPT SLOT PREDICTIONS

--- LAM. SLOT, ZERO PRESS. GRAD.,
A2 = 0.09

----- LAM. SLOT, PRESS. GRAD.,
A2 = 0.09

---- LAM. SLOT, PRESS. GRAD.,
A2 = 0.06

----- TURB. SLOT, ZERO PRESS. GRAD.,
A2 = 0.09

----- TURB. SLOT, PRESS. GRAD.,
A2 = 0.09

HATCHED AREA REPRESENTS SCATTER OF
MACH 6 MEASUREMENTS

(b) $\Lambda = 45^\circ$; $T_t, j/T_t, \infty = 0.6$; $\lambda = 0.048$; $x = 8.26$ cm.

Figure 15.- Concluded.
Figure 16.- Surface flow angle for swept slot injection. 
\[ S = 1.111 \text{ cm}; \; \frac{T_{t,j}}{T_{t,\infty}} = 0.6. \]
Figure 16.— Concluded.

(b) $\Lambda = 45^\circ$; $\lambda = 0.048$. 

Figure 16.— Concluded.
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—National Aeronautics and Space Act of 1958

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