Engineering Notes

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Wall Temperature Control of Low-Speed Body Drag

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Nomenclature

C_f = local skin-friction coefficient
D/D_0 = body drag nondimensionalized by total drag of adiabatic reference
d = maximum body diameter
M_∞ = freestream Mach number
$P_{t,\infty}$ = total freestream or atmospheric pressure
T_w = wall temperature
$T_{t,\infty}$ = total freestream temperature
x = body axis coordinate along the axis of symmetry with origin at stagnation point; positive downstream
α = afterbody conical half-angle
δ^* = boundary-layer displacement thickness

Introduction

METHANE and hydrogen will likely be used as primary fuels for future generations of aircraft; therefore, fuselage drag reduction or flow control using thermal means becomes a possibility due to the existence of the thermal reservoir provided by the fuel.

Previous studies have concentrated on delaying transition by surface temperature control.[1-4] However, transition delay is difficult at fuselage Reynolds numbers as high as 300 million. The present work examines the use of thermal means to control drag under turbulent boundary-layer conditions. This note presents numerical calculations for both skin friction and (unseparated) pressure drag for turbulent boundary-layer flows over a fuselage-like (axisymmetric) body with wall heat transfer. In addition, thermal control of separation on a bluff body was investigated. All calculations were made for high Reynolds number flows at low Mach number and for bodies at zero angle of attack.

Axisymmetric Bodies Considered and Solution Method

Two axisymmetric bodies were studied. Each body had a maximum diameter d of 3.048 m. One geometry was "slender" with fineness ratios on the order of 10, the other was a bluff body with a fineness ratio equal to 3. The streamlined slender body consisted of 1) a 5:1 elliptical nose section, 2 1/2 body diameters in length; 2) a cylindrical midsection, 4 body diameters in length; 3) a truncated elliptical afterbody shoulder; and 4) a conical tail section with an 8-deg half-angle α (see Fig. 1a). The bluff body consisted of a) a 2:1 elliptical nose, 1 body diameter in length; b) a cylindrical midsection; c) a circular-arc shoulder with a radius of 1 1/2 body diameters; and d) a conical tail section with a 30-deg half-angle (see Fig. 1b).

For the current study viscous-inviscid interaction theory (or the viscous correction method) was used to determine the total drag of the slender bodies; boundary-layer theory was used for the calculation of bluff-body separation locations. For the slender body, the viscous correction method updates the theoretical pressure distribution from inviscid theory to account for the presence of the boundary layer through a conventional boundary-layer displacement thickness (δ^*) correction. In the calculation, the inviscid pressure distribution was obtained using the Keller-South potential-flow code.[5] The inviscid pressure distribution was then input to the Harris-Blanchard boundary-layer code[6] to calculate δ^* and the wall shear distributions over the body. After the calculated δ^* distribution was added to the body coordinates, an updated theoretical pressure distribution was determined by repeating the potential-flow calculation. Three iterations between the potential-flow and boundary-layer calculations were required to cause the change in the calculated pressure distribution to become negligible. Frictional and pressure drag were computed by integrating the updated wall shear and pressure distribution, respectively, over the slender body.

Results and Discussion

The computed drag contributions for heating and cooling of the axisymmetric slender body with various degrees of surface heat transfer are shown in Fig. 2. The results are presented in terms of nondimensionalized drag (D/D_0) vs wall-to-total-freestream temperature ratio ($T_w/T_{t,\infty}$) at the following baseline freestream conditions: $M_\infty = 0.2$, $P_{t,\infty} = 100$ kPa, and $T_{t,\infty} = 295$ K. The wall temperature ratio, $T_w/T_{t,\infty}$, was varied between 0.08 and 2. All drag components have been nondimensionalized by total drag value D_0 at the adiabatic reference condition ($T_w/T_{t,\infty} = 1$). Frictional drag was the dominant body drag component (greater than 50% of total drag) at all surface temperature conditions. A total drag reduction as large as 20% with wall heating was obtained at $T_w/T_{t,\infty} = 2$. However, this reduction is not necessarily a net drag reduction for the body because the present study has not included the energy expenditure for heating the wall. Although the use of wall cooling for the slender body did (as expected) tend to reduce the (unseparated) pressure drag slightly by decreasing the displacement thickness, the increase in frictional drag was much greater than the reduction in pressure drag, resulting in an increase in total drag.

The computed drag contributions for partial surface heating and cooling are shown in Figs. 2b-2d. Forebody heating, as shown in Fig. 2b, also resulted in a total drag reduction (up to 13% at $T_w/T_{t,\infty} = 2$). The forebody heat-transfer zone comprised 50% of the total surface area. The total drag increase due to the partial forebody cooling is about 40% less than for the full surface coverage case. Figures 2c and 2d indicate small total drag reductions of 7 and 3% for 50 and 22% afterbody surface heating, respectively, and an increase in total drag for afterbody wall cooling. Although the total drag reduction for the afterbody wall heating cases was smaller than the case of 100% surface heating, the increase in total drag for the two

Received July 30, 1985. This paper is declared a work of the U.S. Government and therefore is in the public domain.

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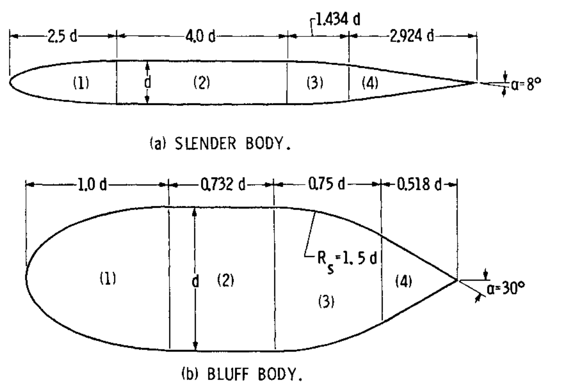

Fig. 1 Geometry of axisymmetric bodies.

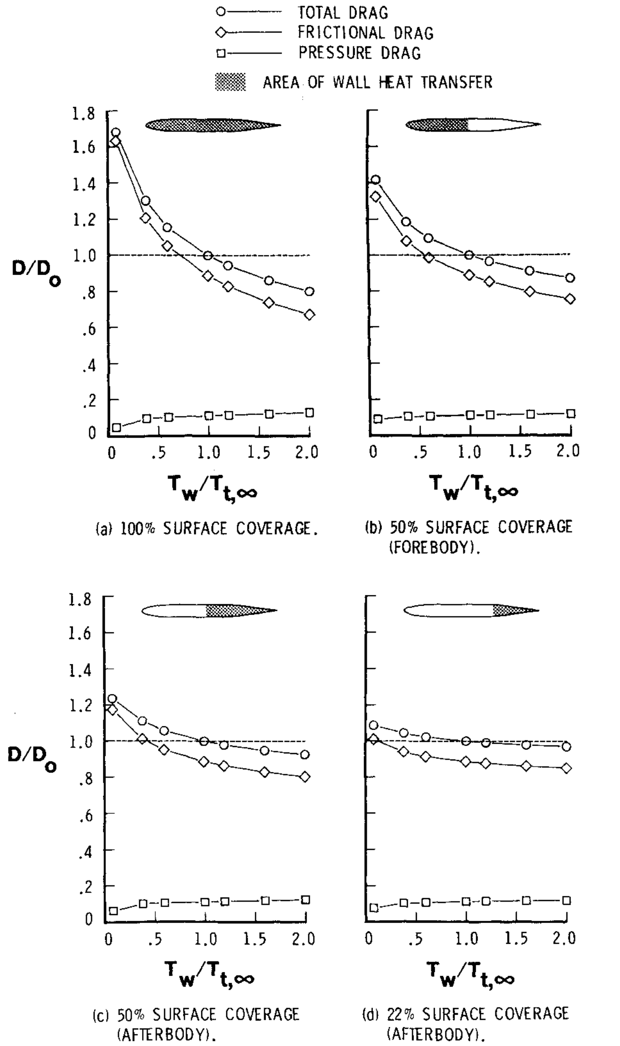

Fig. 2 Influence of full and partial wall heat transfer on the drag of a slender body.

afterbody cooling cases was much smaller than the case of 100% surface cooling. This is important if afterbody cooling is used to delay the afterbody separation point on a bluff body. The frictional drag increase due to wall cooling must be minimized to obtain the maximum benefit from the pressure drag reduction due to the delay in separation on the afterbody.

The computed skin-friction coefficient (C_f) distributions over the bluff body with full and partial surface coverages at

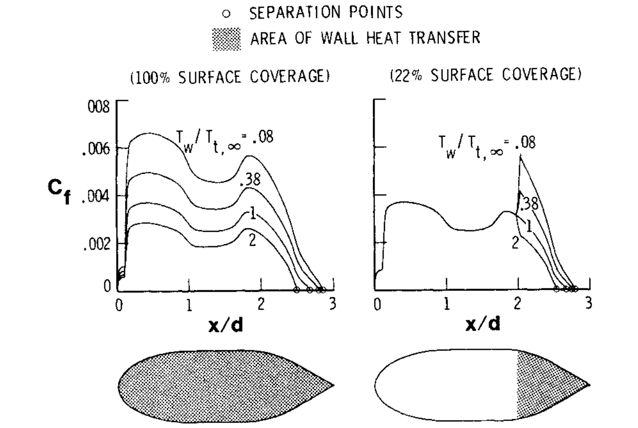

Fig. 3 Comparison of C_f distributions between full and partial thermal control for a bluff body.

baseline freestream conditions are shown in Fig. 3. The resulting C_f distributions over the bluff body clearly show that wall cooling delays flow separation, while wall heating moves separation forward (see also Refs. 7-9). The C_f distributions also show that the partially cooled surfaces delayed flow separation nearly as much as the fully cooled case but with only one-sixth of the frictional drag associated with the fully cooled case.

Conclusion

In summary, the present calculations indicate a "total" drag reduction of up to 20% for wall heating at $T_w/T_{t,\infty} = 2$. For streamlined slender bodies, partial wall heating of the forebody can produce almost the same order of total drag reduction as the full body heating case. For bluff bodies, the separation delay from partial wall cooling of the afterbody is approximately the same as the fully cooled body. The partial wall-cooled body has the additional benefit of lower frictional drag increases due to wall cooling. Therefore, partial wall cooling of the afterbody may have applications in reducing the separation of bluff bodies and the associated pressure drag.

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