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Evaluation of Engineering Heat Transfer Prediction Methods in High-Enthalpy Flow Conditions

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Nomenclature

C = constant, f^n (geometry, nature of boundary layer)
h = enthalpy, J/kg
n = exponent, 0.5 for laminar flow and 0.2 for turbulent flow
Pr = Prandtl number
$\dot{q}$ = convective heat transfer rate, W/m^2
u = velocity, m/s
u_{grad} = velocity gradient, u_e/x in planar/conical flow
θ = momentum thickness, m
μ = viscosity, Ns/m^2
ρ = density, kg/m^3

Subscripts

ad.wall = adiabatic wall/recovery
main = boundary-layer edge (in main flow direction)

Superscript

$*$ = reference enthalpy and local pressure

Introduction

APPROXIMATE heat transfer formulations, based on the reference enthalpy extension of boundary-layer solutions over attached flow regions, are applied to generic configurations in the high-enthalpy flow regime. The boundary-layer edge conditions are provided by Euler computations accounting for thermochemical nonequilibrium effects. Comparison of the coupled Euler/reference enthalpy results to thermochemical nonequilibrium Navier–Stokes predictions and to experimental data from hypervelocity/high-enthalpy wind tunnels shows that the proposed methodology gives reasonable engineering heat transfer estimates in the high-enthalpy, thermochemically active flow regime at a significantly lower cost than that of Navier–Stokes computations.

Prediction Methodology

The heat transfer prediction methodology employed involves analytical expressions for convective heating over stagnation regions, infinite swept leading edges, and planar and conical surfaces, based on (or consistent with) Eckert's[1] reference enthalpy concept. These expressions[2] have been generalized into a unique formulation[3]:

$$\dot{q} = CPr^{-\frac{2}{3}}(\rho^*)^{(1-n)}(\mu^*)^n u_{\text{main}}^{(1-2n)} u_{\text{grad}}^n (h_{\text{ad.wall}} - h_{\text{wall}}) \qquad (1)$$

A well-identified main flow direction is required; therefore, the configuration must be approximated by a series of generic two-dimensional or axisymmetric aerodynamic surfaces. The boundary layer is treated in thermochemical equilibrium; in particular, the reference density and viscosity are computed from the local pressure and reference enthalpy on the basis of a thermochemical equilibrium assumption, and so is the wall enthalpy. This procedure is aimed to provide reasonable estimates for either equilibrium flows or nonequilibrium flows over fully catalytic walls, i.e., with equilibrium wall conditions. Special attention is drawn to the nonapplicability of analytical expressions for the viscosity of air (such as Sutherland's or Keyes' laws) at high temperatures.[4]

Moreover, the formulation of Eq. (1) does not account for abrupt changes in the boundary-layer growth rate caused either by changes in the geometry (and, thus, in the boundary-layer edge conditions) or by the occurrence of laminar–turbulent transition. A typical criterion for matching transitions between different flow situations (with different boundary-layer growth rates) is the continuity of the momentum deficit,[5] $\rho_{\text{main}} u_{\text{main}}^2 \theta$, through which the effective origin of boundary layers over consecutive parts of the vehicle (flow situations) may be determined by appropriate expressions.[2,3]

The required boundary-layer edge conditions are provided by the Euler version of the TINA solver,[6] which accounts for thermochemical nonequilibrium effects but not for any viscous interaction involving a significant modification of the effective body shape by the growth of dominant thick boundary layers. Similarly, regions of shock wave/boundary-layer interaction and significant

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flow separation are not explicitly treated, although adequate heat transfer predictions are made downstream of such regions.

Results

Results are presented for the Electre blunt cone geometry[7] and the axisymmetric representations of the windward centerline of the Hermes spaceplane and the Space Shuttle Orbiter at incidence, known as hyperboloid–flare[7] and Halis axisymmetric configuration (HAC),[8] respectively. The proposed Euler/reference enthalpy predictions are compared with experimental data from the ONERA F4 (Ref. 9) and California Institute of Technology T5 (Ref. 10) high-enthalpy wind tunnels, as well as with predictions from the TINA[6] Navier–Stokes solver (for a fully catalytic wall) and the Zoby et al.[11] formulation. Convergence and grid independence of the Navier–Stokes results for the Electre and hyperboloid–flare configurations have been established in the framework of a code validation workshop.[7] With respect to the high Reynolds number HAC computations, the presented solution is not grid independent insofar as the predicted extent of separation is concerned, but the results over the attached flow regions are converged and grid independent.[10]

Electre Blunt Cone Configuration

The Electre[7] blunt cone configuration provides a simple combination of two generic aerodynamic geometries: a sphere and a shallow-angle cone. Its large nose bluntness dominates the pressure distribution over the body (relative to viscous interaction/boundary-layer growth effects), and thus the proposed Euler-based heat transfer methodology is adequate even at the relatively low Reynolds numbers achieved in high-enthalpy facilities. Heat transfer distributions over the Electre geometry in the flow conditions of the ONERA F4 arc-heated facility[9] are given in Figs. 1 and 2, respectively, for an (assumed) nonequilibrium and equilibrium freestream in the test section. In both cases, the TINA[6] Navier–Stokes nonequilibrium results over the Electre conical afterbody are in very good agreement with the reference enthalpy results for a cone. The Zoby et al.[11] predictions (also based on the Euler-determined edge conditions) are in closer agreement with the planar reference enthalpy results and lower than the Navier–Stokes estimates.

Contrasting Figs. 1 and 2 illustrates the pronounced effect of the freestream thermochemical behavior on the heating of the conical afterbody of the Electre blunt cone: An equilibrium freestream gives significantly higher heating rates than a nonequilibrium freestream, largely due to the lower Reynolds number (and Mach number) in the equilibrium freestream case. This thermochemical effect is well captured by the coupled Euler/reference enthalpy methodology, which yields predictions in good agreement with the respective nonequilibrium Navier–Stokes solutions.

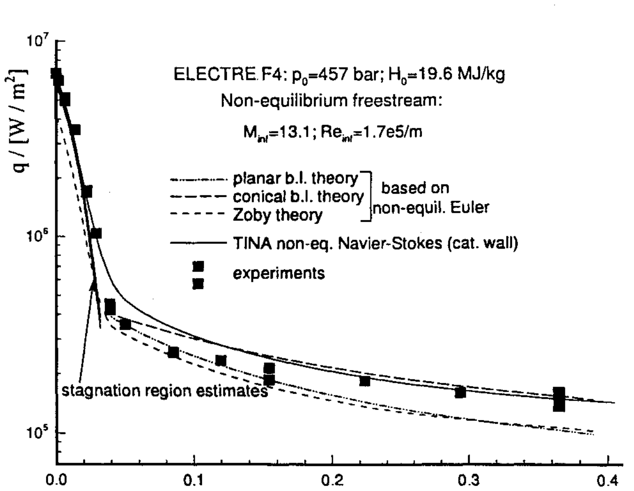

Fig. 1 Comparison of Electre heat transfer distributions in F4 conditions: nonequilibrium computations assuming nonequilibrium freestream.

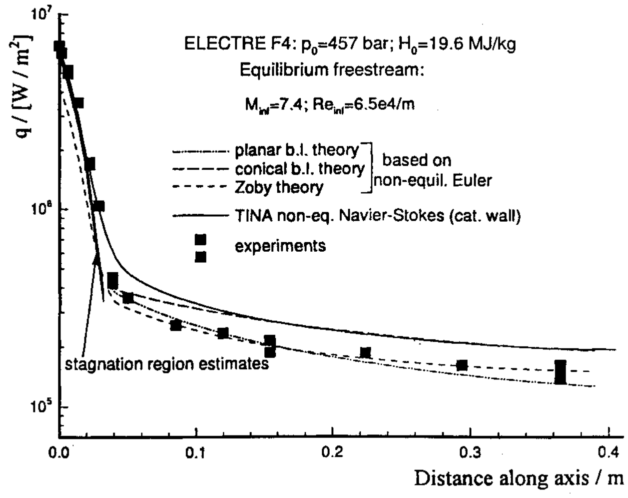

Fig. 2 Comparison of Electre heat transfer distributions in F4 conditions: nonequilibrium computations assuming equilibrium freestream.

The best comparison of the measured data with the Navier–Stokes and conical reference enthalpy estimates is obtained on the assumption of a nonequilibrium freestream in the F4 tunnel, although some significant discrepancies persist between the (sphere–cone) shoulder of the configuration and its half-length. Such discrepancies have prompted the reevaluation of measurements in the F4 facility and the development of novel procedures for the determination of the freestream conditions in the tunnel.[12,13]

With respect to the spherical stagnation region of the Electre configuration, reasonable predictions are made by Eq. (1) for stagnation point heating [Eq. (1) is equivalent to the Fay–Riddell[14] result for unit Lewis number], using various estimates for the stagnation point velocity gradient[3] (Newtonian, Euler, or from the empirical correlation of computational data[13]) with little scatter between them. It is noted that stagnation point heating, contrary to the conical afterbody heating, is not sensitive to the thermochemical behavior of the nozzle flow expansion and freestream of the F4 tunnel because stagnation point heat transfer is dominated by the pitot pressure and flow total enthalpy, both of which are insensitive to freestream (non-) equilibrium effects.[15] Moreover, the assumption of a simple $\cos^{3/2}\phi$ heat transfer variation around a spherical nose[16] is seen to provide adequate estimates around the Electre spherical nose up to $\phi \approx 70$ deg away from the stagnation point.

Hyperboloid–Flare Results

The hyperboloid–flare configuration[7] comprises a (modest-bluntness) spherical nose, a hyperboloid forebody, and a flare, in the vicinity of which shock wave/boundary-layer interaction phenomena and flow separation occur. The forebody is pressure dominated, by virtue of the nose bluntness and large flow deflection, even at low Reynolds number conditions. Important viscous interaction is found only in the vicinity of the hyperboloid–flare junction, where the shock wave/boundary-layer interaction causes significant flow separation (in laminar flow). Consequently, the Euler solution provides a reasonable representation of the boundary-layer edge conditions over the body, except in the near-hinge region, as has been confirmed by comparison of Euler and Navier–Stokes estimates of the pressure distribution.

The measured[9] and TINA[6] predicted heat transfer distributions over the hyperboloid–flare are compared to the estimates from the present Euler/reference enthalpy methodology in Fig. 3. The proposed methodology provides adequate estimates for the heat transfer distribution over the configuration, excluding the low-heating separated flow region but including the high-heating region downstream of reattachment.

HAC

Similar to the hyperboloid–flare case, the HAC configuration in T5 (Ref. 10) flow conditions is pressure dominated, with important

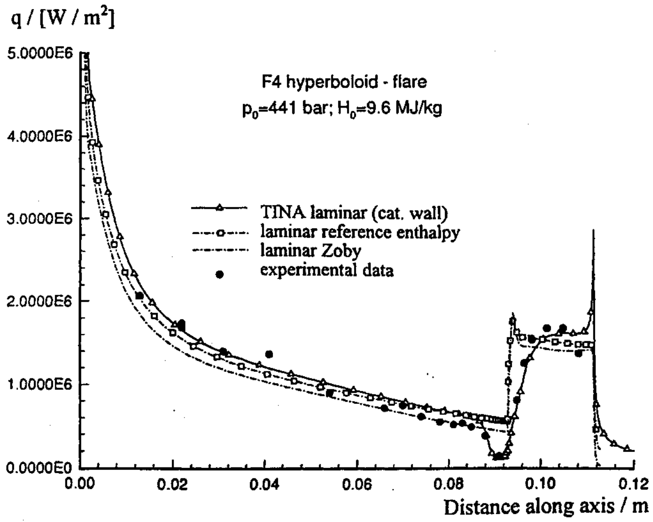

Fig. 3 Measured and computed heat transfer distributions over the hyperboloid–flare configuration in F4 conditions.

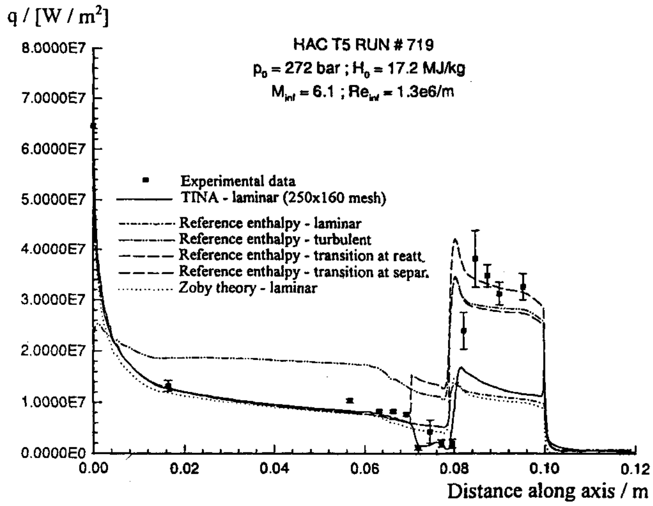

Fig. 4 Measured and computed heat transfer distributions over the HAC in T5 flow conditions.

viscous interaction encountered only in the vicinity of the hinge of the deflected flare where flow separation occurs in laminar oncoming flow. This has, again, been confirmed by comparison of the Euler and Navier–Stokes pressure distributions.

Typical measured and computed heat transfer distributions over the HAC are shown in Fig. 4. Overall, a good comparison is seen between the (laminar) Navier–Stokes and laminar reference enthalpy predictions, and also good agreement is found between all laminar predictions and the measurements upstream of the onset of the shock wave/boundary-layer interaction. The measured heat transfer distribution over the separated zone is captured only by the laminar Navier–Stokes result.

Downstream of the interaction, the experimental data are well approximated by the turbulent reference enthalpy predictions assuming laminar–turbulent transition in the vicinity of the hinge/reattachment/location of peak heating. The occurrence of laminar–turbulent transition in the most unstable flow reattachment region is consistent with numerous observations of transition promotion by shock wave/boundary-layer interactions and attributed[17,18] to the local adverse pressure gradient and flow concavity combined with the high Reynolds number of this case (relative to the preceding, lower Reynolds number hyperboloid–flare case in F4 conditions, where the flow remained fully laminar).

Conclusions

An approximate heat transfer prediction methodology, based on the reference enthalpy formulation coupled to thermochemical nonequilibrium Euler solutions, has been applied to generic configurations in high-enthalpy flow conditions. Comparisons with thermochemical nonequilibrium Navier–Stokes solutions and experimental data from high-enthalpy wind tunnels have shown that the proposed methodology provides reasonable engineering estimates (typically within less than 20%) for the heat transfer distribution over pressure-dominated configurations (not exhibiting strong viscous interactions) at a cost much lower than that of Navier–Stokes solutions.

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