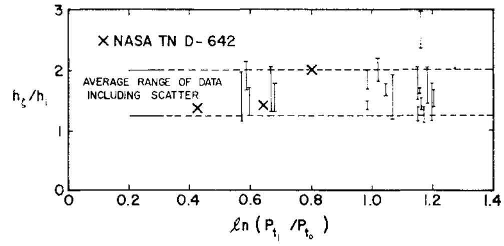

Fig. 3 Correlation of heat-transfer rates at shock impingement.

seen immediately that the nondimensional heat-transfer coefficient ratio h_ζ/h_0 is not constant, but exhibits a variation which is nearly linear. Results from Ref. 5 are also shown and are in good agreement with the present values. Impingement heating rates for body expansion shock waves (at negative α) are not shown. The calculation of the correlation parameter includes the effects of α and fin sweep on the shock angles. It was found that $\ln(P_{ti}/P_{t0})$ is very sensitive to small changes in shock angles and affects the results accordingly. As an example, the initial determination of the correlation parameter for the wedge fins gave results in considerable disagreement with the results for the cylindrical fins. An analysis to include leading-edge and boundary-layer displacement effects on the wedge fin shock angles gave a possible range of the correlation parameters such that agreement with Fig. 2 could be obtained, depending on the assumptions made. This uncertainty raises many questions which have not been solved. The results for the wedge fins have been plotted arbitrarily at the same value of $\ln(P_{ti}/P_{t0})$ obtained for the cylindrical fins (slightly offset in Fig. 2 to indicate the data). Substantial agreement is obtained by this means. The question of a valid correlation is further accentuated by the following apparent anomaly in the variation of $\ln(P_{ti}/P_{t0})$ with α: the correlation parameter passed through a maximum at α between 10° and 15°. The variation of h_ζ/h_0 with $\ln(P_{ti}/P_{t0})$ would thus not necessarily be identical with that due to α; the variation of h_ζ/h_0 with α was approximately linear, within the data scatter.

It is true that the effects of α are to produce a range of relative shock strengths and of the correlation parameter. It is also true, however, that increasing α gives a constantly increasing entropy level inside the body shock. Since the heat-transfer rates can be specified as a function of the entropy magnitude, the possibility exists that the impingement heating could vary as some function of the entropy level as well as the local gradient that produces a region of high vorticity. The predicted heat-transfer rates inside the body shock can be shown to be much higher than those outside and to exhibit increasing values as α increases. The correlation in Fig. 3 was then made to show the variation of h_ζ/h_i vs $\ln(P_{ti}/P_{t0})$. This shows h_ζ/h_i to be approximately constant, where $1.4 < h_\zeta/h_i < 2.0$. It thus appears that the variation in peak heating at impingement as depicted in Fig. 2 is not a variation due to the vorticity effects, but rather due to α.

Concluding Remarks

It is implied from the experimental results that the increased local heating exhibits no discernible variation with shock strength (i.e., M) or Re in the experimental range. The distributions of heating on the fins away from impingement agree reasonably well with current simple methods of prediction. Since the region affected by shock impingement is very localized on the geometries tested, this means that, for application to similar shock-impingement cases where the vehicle may be very large, the thermal protection required for the impingement area may be quite small in comparison to the total thermal protection required.

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An Improved Technique for Obtaining Quantitative Aerodynamic Heat-Transfer Data with Surface Coating Materials

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Nomenclature

A	=	temperature at which phase change occurs
$\hat{A}$	=	temperature condition $(A - T_i)/(T_{aw} - T_i)$
h	=	aerodynamic heat-transfer coefficient, h_0 = reference value for a sphere of radius equal to model base radius, h_s = stagnation-point value
k	=	thermal conductivity
l	=	allowable depth of heat penetration
M_∞	=	freestream Mach number
p	=	parameter $(h/k)(\alpha t)^{1/2}$
r	=	spherical radius
$R_{\infty,D}$	=	freestream Reynolds number based on maximum model diameter
t	=	time, t_d = thermal diffusion time
T	=	temperature, T_{aw}= adiabatic wall temperature, T_i = initial temperature of model
x	=	distance normal to back surface of bell-shaped configuration along axis of symmetry
y	=	distance normal to model surface
α	=	thermal diffusivity

Introduction

In Ref. 1 a method for obtaining quantitative aerodynamic heat-transfer data by the use of a visible phase-change coating is described briefly. The coating materials[2] undergo a phase change from an opaque solid to a clear liquid at known temperatures with an accuracy of ±1% which is independent of both ambient pressure and heating rate.‡ This note further discusses the accuracy of this method and presents additional experimental results.

Technique

The phase-change time patterns are recorded by motion-picture photography with precise framing rates. A transient heat-transfer technique is used whereby the tunnel is brought to the desired stagnation conditions, and the camera is started before the model is injected into the test section. The injection time (the time from which the model first encounters

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‡ The phase-change materials used are called Tempilaq and are sold by the Tempil Corporation of New York.

the wind-tunnel boundary layer until it reaches the desired position in the uniform flow) is 0.05 sec. Lines of constant h are located by superimposing photographs of the phase-change patterns and those of a "stand-in" model that is marked with a reference grid. The photographs are made high-speed 35-mm black-and-white film using a special time-study data camera and stroboscopic flash lamps synchronized with the camera shutter. Although these lights had a high intensity, the duration of a single flash was only 25 μsec, so that the total "on time" of the lights at the highest framing rate (30 frames/sec) was only 0.75×10^{-3} sec/second of test time, or less than 0.1%, thus eliminating heat-radiation problems that would occur with high-intensity photoflood lights.

The relationship between the aerodynamic heat-transfer coefficient and the other test parameters is determined from the solution of the equation governing the transient one-dimensional flow of heat:

$$\partial T/\partial t = \alpha(\partial^2/\partial y^2) \tag{1}$$

The initial and boundary conditions that most nearly describe the actual tunnel transient tests are

$$T(y, 0) = T_i \text{ (model initially isothermal)} \tag{2}$$

$$T(\infty, t) = T_i \text{ (model acts as semi-infinite slab)} \tag{3}$$

$$\partial T(0, t)/\partial y = [T_{aw} - T(0, t)]h/k \tag{4}$$

It is assumed that 1) the phase-change coating is at the surface temperature; 2) the surface is subjected to an instantaneous step in h at time zero, and h is invariant with time [Eq. (4)]; and 3) the thermal diffusivity of the wall material is invariant with temperature [Eq. (1)]. The solution of Eq. (1) with the stated boundary conditions can be written[3]

$$\hat{A} \equiv (A - T_i)/(T_{aw} - T_i) = 1 - e^{p^2} \operatorname{erfc} p \tag{5}$$

where $p = (h/k)(\alpha t)^{1/2}$, and A is the temperature at which the phase change occurs. The time required for the phase change should be large compared to model injection time but short compared to the thermal diffusion time of the wall because of boundary condition (3). The thermal diffusion time t_d is essentially independent of h and is given approximately by the relation $\alpha t_d/l^2 = 0.2$. In order to satisfy the one-dimensional heat-conduction requirements, the allowable l must be small compared with pertinent model dimensions (wall thickness, nose radius, etc.).

Desired model features include 1) small α to insure reasonably long t_d with acceptable accuracy from the one-dimensional semi-infinite slab solution; 2) ability to withstand high-injection accelerations, thermal shock, and relatively high temperatures; 3) uniformity of material; 4) a surface that is impervious to the test gas and to the thinner used and is dark colored to provide sufficient contrast with the light colored unmelted coating.

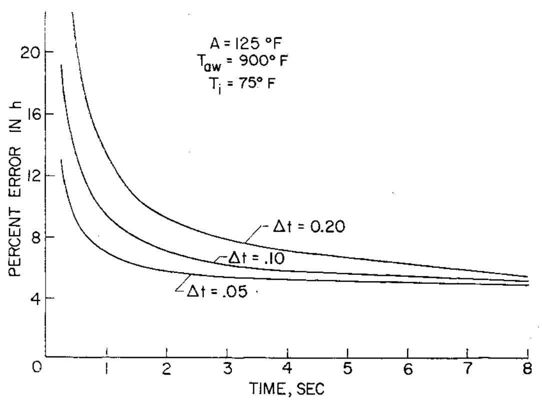

Fig. 1 Error in heat-transfer coefficient due to errors in thermal properties, time, and phase-change temperature.

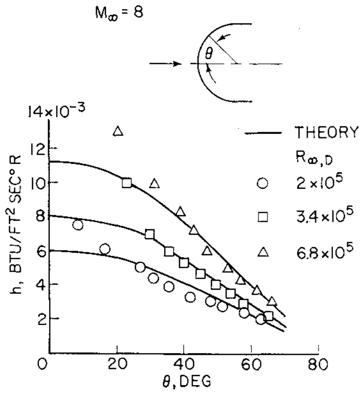

Fig. 2 Heat-transfer distribution on sphere.

The models used for the phase-change data reported herein were constructed from a Fiberglas-reinforced plastic, for which the properties were measured with the following accuracies[4]: specific heat, ±2%; thermal conductivity, ±3%; and density, ±1%. The maximum percent error in h has been calculated by assuming these errors in the thermophysical properties and an error in phase-change temperature of ±1% (value quoted by manufacturer). The results are shown in Fig. 1 as a function of time for $A = 125°$F, $T_{aw} = 900°$F, $T_i = 75°$F, and three different values for error in initial time (Δt). An error in time of 0.1 sec was thought by the authors to be the maximum possible error with the technique used herein. The maximum percent error in h is independent of the magnitude of h; its time variation depends only on Δt. For most hypersonic facilities the difference, $T_{aw} - T_i$, is very large compared to any possible error in T_{aw}, so that T_{aw} can be neglected as a source of

no flow

early time

later time

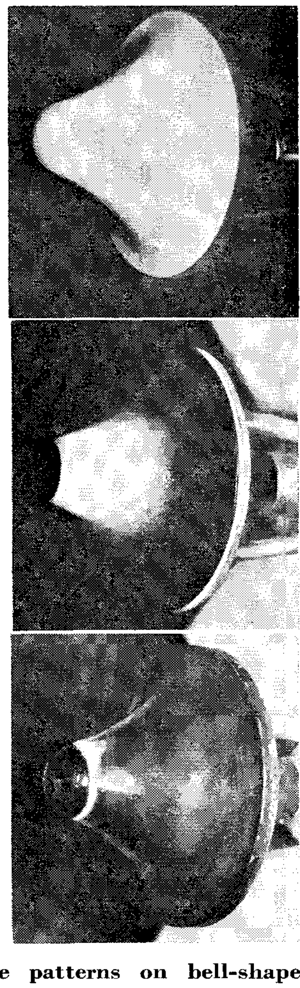

Fig. 3 Phase-change patterns on bell-shaped model.

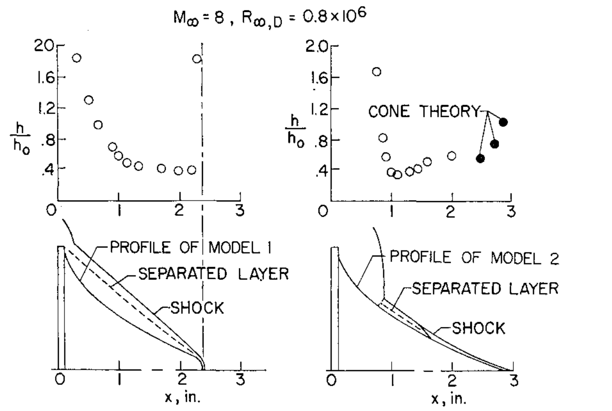

Fig. 4 Heat-transfer distributions on bell-shaped configurations.

error. However, the difference, $A - T_i$, is not always large, so that the error in h due to the error in A becomes larger for coating materials having smaller values of A. Therefore, it is essential to cool the model to near room temperature for each run, particularly for coatings with low values of A. Also, since the model must be made from good insulating material, care must be taken to avoid errors due to radiation from external sources to the model.

Experimental Results

The data presented herein were obtained in the Langley Mach 8, variable-density tunnel, which is described briefly in Ref. 5. A 4-in.-diam hemisphere was tested at three values of $R_{\infty, D}$. The 200°F phase-change material was used. The h distributions obtained are shown in Fig. 2 and are compared with a theory for which a modified Newtonian pressure distribution was used with the method of Ref. 6 to determine the stagnation-point value and the method of Ref. 7 to determine the distribution. Agreement with theory is relatively good except for the data nearest the stagnation point for each curve. These data correspond to the earliest time (0.2 sec) for which data could be reduced; therefore, the error in h due to any error in determining the initial time is large.

Two bell-shaped configurations were tested by the phase-change method. They had rather extensive separation regions with reattachment somewhere on the flare. Sample photographs of the phase-change patterns obtained on model 1 are shown in Fig. 3. Notice the sharply defined regions of very low heat-transfer rate just downstream of the nose. The measured heat-transfer coefficients are shown in Fig. 4 as a function of distance and compared with sketches of the flow patterns. In Fig. 4 for the blunt-nosed model, h varies by a factor of 4.5 over a surface distance of only 0.05 in. It is doubtful that heat-transfer rates could be measured accurately by the thermocouple-calorimeter technique in regions subjected to such large gradients. In Ref. 8, the phase-change coating method was used to measure the heating rates in regions near holes, protuberances, and reaction-control jets on a 4-in.-diam model of the Apollo command module. The fine details in heat-transfer distributions obtained in the interference regions of this model are considered noteworthy for such small model dimensions.

It is concluded that this technique can produce quantitative heat-transfer data on arbitrary shapes. It should be useful for complex configurations that would be difficult to instrument with thermocouples and for configurations subjected to interference effects of unknown extent and location.

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Orbital Elements from the Doppler Tracking of Four Satellites

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IN Ref. 1 we presented the initially determined orbital elements from the Doppler tracking of three satellites: 1961 $\alpha\eta$1 at an inclination of 33°, 1962 $\beta\mu$1 at 50°, and 1961 o 1 at 67°. These elements were determined with a model of

Table 1 Orbital elements of 1963 49B

t_p, days since Jan. 0.0	$10^6 e$	$10^4(i-89°)$ Year = 1964	ω degrees	Ω degrees	N
25.00464877	4349	9619	137.393	102.0492	673
29.02228416	4503	9617	128.255	102.0342	727
35.04880728	4681	9593	114.889	102.0096	808
54.02143615	4724	9577	74.004	101.9319	1063
62.05681946	4510	9548	56.303	101.8965	1171
69.05044640	4219	9549	40.264	101.8657	1265
73.06797157	4017	9533	30.615	101.8473	1319
79.01969823	3685	9540	15.549	101.8200	1399
83.03696314	3446	9537	4.648	101.8019	1453
90.02957202	3029	9522	343.720	101.7688	1547
94.04628710	2808	9535	330.182	101.7501	1601
98.06277245	2621	9529	315.525	101.7315	1655
103.93828219	2429	9530	291.877	101.7037	1734
105.05384391	2406	9528	287.189	101.6985	1749
111.89581988	2396	9538	257.767	101.6682	1841
113.01137809	2415	9530	253.064	101.6630	1856
119.92810762	2637	9549	225.199	101.6317	1949
121.04379066	2686	9550	221.103	101.6269	1964
125.06035623	2885	9552	206.861	101.6094	2018
129.00278583	3111	9547	194.076	101.5916	2071
133.01979740	3355	9560	182.002	101.5743	2125
137.03698440	3592	9573	170.779	101.5576	2179
141.05431728	3825	9565	160.265	101.5402	2233
143.88138990	3989	9564	153.161	101.5289	2271
149.01488823	4245	9584	140.901	101.5071	2340
157.05011832	4559	9577	122.629	101.4743	2448

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