Engineering Notes

Analytical Formulation for Thermal Protection of Active Cooling Nosetips

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Nomenclature

H = total enthalpy, J/kg
h = static enthalpy, J/kg
L_v = vaporized latent heat, J/kg
M = total coolant mass, kg
$\bar{M}$ = molecular weight
$\dot{M}$ = coolant flow rate, kg/s
$\dot{m}$ = coolant mass flux, kg/m²s
p = pressure, atm
q = heat flux, J/m²s
$\bar{R}$ = universal gas constant, 8341.3 J/kg mole k
$\bar{R}$ = nose radius, m
S = surface area of porous matrix, m²
T = temperature, K
t = time, s
Z = compressed coefficient
Γ = permeability, Darcy
μ = dynamic viscosity, kgs/m²
ρ = density, kg/m³

Subscripts

a = air
c = coolant
E = vaporous species of coolant
ex = external flow conditions
g = gas
in = interior flux conditions
l = liquid
l_a = laminar flow
r = radiation
s = stagnation-point condition
t = turbulent flow
w_1 = external wall
w_2 = interior wall

Introduction

THE active cooling nosetip design must determine the weight of the coolant and driver materials and select suitable coolant preliminary and applicable porous structures. Characterization of the porous structure for self-contained adaptive transpiration (SCAT) was achieved by determining liquid metal permeability at elevated temperature and pressure using liquid coolant. The subjects requiring considerably more analytical and/or experimental investigation are noted.

Theoretical studies can be adopted to solve some problems such as flow characteristics of porous media. Interaction and interchange of mass, momentum, and energy exist within the surface cooling flow, porous structures, and gas boundary layer due to transpiration-cooled nosetips at high heat transfer rates and stagnation pressure. Therefore, the permeable flow of transpiration-cooled nosetips is more complicated than general permeable flow. It is necessary to solve a partial differential equation group with four equations and to calculate the dual iteration of internal pressure, permeability, and wall thickness in numerical calculations. Therefore, it is necessary to explore a simplified calculation. One method will be provided in this Note.

Simplified Assumption

In the SCAT concept, coolant and driver materials are stored within the nosetip shell (reservoir). During re-entry, the majority of the coolant is molten and the driver vapor occupies a larger portion of the reservoir volume. A considerable amount of fluid convection takes place within the liquid coolant due to the deceleration load and temperature difference between forward and aft nosetip components. This results in enhanced heat transfer between the coolant and shell and within the coolant itself. For internal flow of the SCAT porous matrix, the following simplified assumptions are used.

1) The coolant flow in porous media is a one-dimensional, quasi-steady-state flow. Coolant chemical reaction with the porous media does not occur.

2) The liquid coolant is not vaporized in the reservoir but is vaporized wholly at the external surface of the porous nosetip, i.e., the liquid layer is thinnest or absent at the surface of the porous nosetip.

3) The thickness of the porous matrix is reduced, and the change of the thermal enthalpy in the porous wall of liquid coolant is much smaller with the vaporized heat in the reservoir. As it approaches steady-state flow, it yields

$$\frac{T_{w_1} - T_{w_2}}{T_{w_1}} \ll 1$$

Deduction of Calculative Formulas for Thermal Protection

The simplified calculation formulas of internal flow parameters are deduced based on previous simplified assumptions and energy equations of one-dimensional quasi-steady-state flow.

Calculate Formulas of Coolant Flow Rate

Based on the mechanism of transpiration cooling thermal protection and simplified assumptions 1 and 2, the one-dimensional, quasi-steady-state energy equation is deduced to be

$$q_{w_1 g} - q_{rw_1} - \dot{m} h_{w_1(g)} + \dot{m} h_{w_2(l)} = q_{sw_2} \qquad (1)$$

Assuming that the influence for output coolant flow of internal plenum pressure is smallest and internal plenum pressure and

temperature are approaching constant, then $q_{sw_2} \approx 0$. Because the wall temperature of the transpiration cooling nosetip is lower, q_{rw_1} in Eq. (1) may be negligible. The substitution of $L_v, h_{w_1(l)}$ into Eq. (1) yields

$$q_{w_1g} - \dot{m}\left[h_{w_1(g)} - h_{w_1E} + C_{pl}\left(T_{w_1} - T_{w_2}\right) + L_v\right] = 0 \quad (2)$$

where

$$\dot{m}L_v = \dot{m}\left[h_{w_1E} - h_{w_1(l)}\right] \quad (3)$$

$$h_{w_1(l)} = h_{w_2(l)} + C_{pl}\left(T_{w_1} - T_{w_2}\right) \quad (4)$$

$$\Delta H \equiv h_{w_1(g)} - h_{w_1E} + C_{pl}\left(T_{w_1} - T_{w_2}\right) + L_v \quad (5)$$

then

$$q_{w_1g} - \dot{m}\Delta H = 0 \quad (6)$$

According to simplified assumption 3, $C_{pl}(T_{w_1} - T_{w_2}) \ll L_v$, then $C_{pl}(T_{w_1} - T_{w_2})$ generally may be negligible. To prove $[h_{w_1(g)} - h_{w_1E}] \ll L_v$, $h_{w_1(g)} - h_{w_1E}$ may be negligible, a detailed analysis of the following is necessary:

$$q_{w_1g} = \left(k\frac{\partial T}{\partial y}\right)_{w_1} - \left(\sum j_i h_i\right)_{w_1} \quad (7)$$

$$w_i = \dot{m}K_{iw_1} + j_{iw_1} \qquad i \neq E$$

$$\dot{m} + \dot{w}_E = \dot{m}K_E + j_E \quad (8)$$

Substituting Eqs. (7) and (8) into Eq. (2),

$$\left(k\frac{\partial T}{\partial y}\right)_{w_1} - \sum_{i \neq E}(j_i + \dot{m}K_i)_{w_1}h_{iw_1} - (j_E + \dot{m}K_E - \dot{m})_{w_1}h_{w_1E}$$

$$- \dot{m}L_v - \dot{m}C_{pl}\left(T_{w_1} - T_{w_2}\right) = 0 \quad (9)$$

When the chemical reaction has not occurred at the porous wall ω_1, the second and third terms are equal to zero in Eq. (9). The enthalpy of injected species h_{ω_iE} is separated by two terms:

$$h_{w_1E} = \left(L_v + h_E^0\right) + h_{w_1E}^* \quad (10)$$

Substitution of Eqs. (7), (8), and (10) into Eq. (2) then yields

$$\left[k\frac{\partial T}{\partial y} - \sum_{i \neq E} j_i h_i - j_E h_E^*\right]_{w_1} - \dot{m}\left[\sum_{i \neq E} K_i h_i + K_E h_E^*\right]_{w_1}$$

$$+ \dot{m}h_{Ew_1}^* - \dot{m}\left[L_v + C_{pl}\left(T_{w_1} - T_{w_2}\right)\right] = 0 \quad (11)$$

where

$$q_{w_1g}^* = \left[k\frac{\partial T}{\partial y} - \sum_{i \neq E} j_i h_i - j_E h_E^*\right]_{w_1} \quad (12)$$

Then, Eq. (11) becomes

$$q_{w_1g}^* - \dot{m}\left[\left(\sum_{i \neq E} K_i h_i + K_E h_E^*\right)_{w_1} - \left(1 - K_{Ew_1}\right)h_{w_1E}^*\right.$$

$$\left. + L_v + C_{pl}\left(T_{w_1} - T_{w_2}\right)\right] = 0 \quad (13)$$

where

$$\Delta H^* \equiv \left[\left(\sum_{i \neq E} K_i h_i + K_E h_E^*\right)_{w_1} - \left(1 - K_{Ew_1}\right)h_{w_1E}^*\right.$$

$$\left. + L_v + C_{pl}\left(T_{w_1} - T_{w_2}\right)\right] \quad (14)$$

The $h_{Ew_1}^* (= h_{w_1E}^*)$ of Eq. (14) in the first and second right-hand terms do not include the contribution of L_v and h_E^0. Generally, $h_i \ll L_v$; therefore, in the preceding calculative formula of heat flux for injected influence, the ΔH may be approximately as follows:

$$\Delta H^* \approx L_v + C_{pl}\left(T_{w_1} - T_{w_2}\right) \approx L_v \quad (15)$$

Combining Eqs. (13) and (15) yields

$$\dot{m} = \frac{q_{w_1g}^*}{\Delta H^*} = \frac{q_{w_1g}^*}{L_v} \quad (16)$$

The formulas of approached heat flux in Ref. 1 have been employed in the present analysis. Substituting the formulas of approached heat flux in Ref. 1 into Eq. (16) yields

$$\dot{m}_{la} = \frac{q_{w_0}}{L_v\left[1 + 0.6(\bar{M}_a/\bar{M}_c)^{\frac{1}{3}}\right]} \quad (17a)$$

$$\dot{m}_t = \frac{q_{w_0}}{L_v\left[1 + 0.2(\bar{M}_a/\bar{M}_c)^{\frac{1}{10}}\right]} \quad (17b)$$

Calculative Formulas of Total Coolant Mass

According to simplified assumptions 1 and 3, Eqs. (17a) and (17b) can be integrated:

$$m_{la} = \int_{t_0}^{t}\int_{s_0}^{s}\frac{\varphi q_{w_0}}{L_v\left[1 + 0.6(\bar{M}_a/\bar{M}_c)^{\frac{1}{3}}\right]}\,\mathrm{d}s\,\mathrm{d}t \quad (18a)$$

$$m_t = \int_{t_0}^{t}\int_{s_0}^{s}\frac{\varphi q_{w_0}}{L_v\left[1 + 0.2(\bar{M}_a/\bar{M}_c)^{\frac{1}{10}}\right]}\,\mathrm{d}s\,\mathrm{d}t \quad (18b)$$

Calculative Formulas of Internal Flux Pressure for Nosetip Shell

According to simplified assumption 2, the equation of coolant mass flow rate m is deduced by the one-dimensional momentum equation directly as

$$\dot{m} = -\Gamma\frac{\rho}{\mu}\frac{\mathrm{d}p}{\mathrm{d}x} \quad (19)$$

For the liquid coolant where the density is constant, integration of Eq. (19) leads to a desired internal flux pressure. For the gas coolant where the density is not constant, the integration of Eq. (19) results in the gas state equation being employed, where

$$p/\rho = ZRT \quad (20)$$

$$\dot{m}\int_{\bar{R}_{in}}^{\bar{R}_{ex}}\mathrm{d}\bar{R} = -\int_{p_{in}}^{p_{ex}}\frac{\Gamma p}{ZRT\mu}\,\mathrm{d}p \quad (21)$$

Therefore

$$\dot{m} = \left[\frac{\Gamma\left(p_{in}^2 - p_{ex}^2\right)}{2\mu ZRT\left(\bar{R}_{ex} - \bar{R}_{in}\right)}\right] \quad (22)$$

The calculative formulas of internal flux pressure for the nosetip shell for the gas coolant is

$$(p_{in})_{la} = \left\{\frac{2\mu ZRT(\bar{R}_{ex} - \bar{R}_{in})q_{w_0}}{\Gamma L_v\left[1 + 0.6(\bar{M}_a/\bar{M}_c)^{\frac{1}{3}}\right]} + p_{ex}^2\right\}^{\frac{1}{2}} \quad (23a)$$

$$(p_{in})_t = \left\{\frac{2\mu ZRT(\bar{R}_{ex} - \bar{R}_{in})q_{w_0}}{\Gamma L_v\left[1 + 0.2(\bar{M}_a/\bar{M}_c)^{\frac{1}{10}}\right]} + p_{ex}^2\right\}^{\frac{1}{2}} \quad (23b)$$

Table 1   Partial calculation results (change of pressure and coolant flow in re-entry process, time from 76.2 km, Tl coolant)

t	p_{in}, present	p_{in}, Ref. 2	Relative difference, %	$\dot{M}$, present	$\dot{M}$, Ref. 2	Relative difference, %
17.5	20.50	21.50	4.65	0.036	0.034	−5.88
18	40.00	45.00	11.11	0.054	0.049	−10.20
19	100.00	105.00	4.76	0.207	0.202	−2.48
20	195.00	200.00	2.50	0.464	0.463	−0.22
20.5	234.80	262.50	10.55	0.563	0.540	−4.26
22	212.48	212.50	0.01	0.473	0.459	−3.05
23	193.00	200.00	3.50	0.477	0.454	−5.07
24	187.00	190.00	1.57	0.346	0.360	−3.89
25	175.00	177.00	1.13	0.383	0.374	−2.41

Table 2   Partial calculation results of M, Δp, and p_{in} for four coolants

Coolant	M	Δp	p_{in}
Tl	4.5	92.90	192.90
In	2.01	62.44	162.44
Sn	1.92	70.55	170.55
H_2O	2.92	494.62	594.66

and for liquid coolant is

$$(p_{in})_{la} = \frac{q_{w_0} \mu (\bar{R}_{ex} - \bar{R}_{in})}{\rho \Gamma L_v \left[1 + 0.6(\bar{M}_a/\bar{M}_c)^{\frac{1}{3}} \right]} + p_{ex} \qquad (24a)$$

$$(p_{in})_t = \frac{q_{w_0} \mu (\bar{R}_{ex} - \bar{R}_{in})}{\rho \Gamma L_v \left[1 + 0.2(\bar{M}_a/\bar{M}_c)^{\frac{1}{10}} \right]} + p_{ex} \qquad (24b)$$

Calculation Examples

For a check of the validity of the simplified method, a computational example is given. According to the parameters of the re-entry orbit, the thermal protection of SCAT with four different physical character coolants is calculated.

The partial calculated results are given in Tables 1 and 2. It can be seen in Table 1 that, in the re-entry process, the present calculative results agree fairly well with the numerical computation of Ref. 2 for the pressure and coolant of thallium.

Table 2 shows the total coolant mass flow $M_{Sn} < M_{In} < M_{H_2O} < M_{Tl}$ and the internal pressure $p_{In} < p_{Sn} < p_{Tl} < p_{H_2O}$. The results of the computational example show that the thermal protection effect of coolant In is best and that the thermal protection effect of coolant H_2O is worst because the internal pressure (p_{H_2O}) is too high, destroying the porous nosetip. Thus, H_2O is an unacceptable coolant.

Conclusions

From the preceding results and analyses of calculation examples, we can derive the following conclusions.

1) The theoretical analysis method based on three simplified assumptions is dependable and accurate.

2) The simplified calculative results agree fairly well with the numerical computation of Ref. 2. The difference between the present simplified calculation and the numerical computation of Ref. 2 is about $\pm 0.01 \sim \pm 11\%$.

3) The present calculative method can be satisfactorily used for prediction before a ground simulation experiment, and it can be done at the request of an engineering application department.

References

[1]Fogaroli, R. P., and Laganelli, A. L., "The Effect of Mach Number and Wall Temperature on Turbulent Heat Blockage Resulting from Mass Injection," AIAA Paper 77-784, July 1977.

[2]Grinberg, I. M., Stellrecht, D. E., Whitacre, G. R., and Bagby, F. L., "Development of SCAT Nosetip Concept for Advanced Reentry Vehicles," U.S. Naval Surface Weapons Center, Silver Spring, MD, Jan. 1976.

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Performance of Diamond-Coated Silicon Nitride Bearings

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Introduction

S OLID lubrication of bearings for space applications, such as precision pointing mechanisms, has been proposed in recent years with the use of synthetic diamond coatings. Diamond is a reasonable candidate for a low-friction coating because it possesses exceptional corrosion resistance and a low coefficient of friction (except at ultra-high vacuum) with the highest values of hardness and stiffness.[1] In addition to the lubrication properties of diamond, the coating may improve the chemical/mechanical wear resistance of the base material. Coatings are applied to bearing elements for improving performance and prolonging life by enhancing the resistance to substrate spalling (galling) and lubricant degradation.[2]

Diamond may be a suitable low-friction and protective coating for bearings because it is available in thin film form by chemical vapor deposition (CVD) methods at low pressures (130–1.3×10^4 Pa) and modest temperatures (600–$1000°C$) (Ref. 3). An important aspect of the CVD process is the conformal coating of complex shapes, such as spheres. Much of the effort in our laboratory was directed toward obtaining surface characteristics of the diamond coating that are consistent with precision (silicon nitride) bearing components, such as the balls and rings.[4] A conformal diamond has been obtained on 1–25-mm-diam silicon nitride balls.

A number of refractory materials can be diamond coated, such as most ceramics. However, as diamond growth is achieved at lower temperatures, useful coatings on aerospace materials are available.[5] Silicon nitride was selected for the base material of the bearing elements because of its high melting point, availability as precision bearings, and ongoing efforts to qualify silicon nitride hybrid (metal and ceramic) and all-ceramic bearings for aerospace applications. In addition, a useful diamond coating cannot presently be achieved on common aerospace bearing materials, such as 52100 steel. The results of a bench scale evaluation of diamond-coated silicon nitride bearings operating without additional lubrication is noted in the following.

Experiment

Completed and assembled R8 silicon nitride bearings were obtained consisting of Norton NBD-200 inner and outer rings and Cerbec balls also made of NBD-200. The bearing outer diameter is 3.1 cm with 4-mm-diam balls. Components were prepared for diamond deposition by disassembling the bearing, abrading with 0.1-μm diamond powder, and rinsing in solvents. The diamond scratching procedure is commonly used in CVD diamond deposition processes for enhancing the nucleation density. Diamond growth was achieved in a microwave plasma system operating at 2.45 GHz with a 1% concentration of methane in hydrogen. The total flow rate was 200 standard cm³ min^{-1} with a pressure of 6.7×10^3 Pa. The sample temperature was measured by a two-color optical pyrometer as approximately $800°C$.

Coated components (rings and balls) were examined by optical and scanning electron microscopy (SEM) to determine the coating coverage and microstructure. Raman spectroscopy was performed to determine the diamond quality, providing a nondestructive (optical) evaluation of the coating quality because diamond has a characteristic peak at 1332 cm^{-1}. The film stress can also be determined by Raman spectroscopy as described elsewhere.[6] Finally, profile

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