

Spanwise non-uniform surface temperature distributions for high-speed boundary layer transition control

Kazuki Ozawa ^{*}, Chengwei Xia [†], Georgios Rigas [‡], and Paul Bruce [§]
Imperial College London, London, SW7 2AZ, United Kingdom

Laminar-to-turbulent boundary layer transition leads to increased friction and aerodynamic heating in hypersonic vehicles. This study proposes a novel methodology to control transition in hypersonic wall-bounded flows by introducing spanwise non-uniform temperature surface distributions. The proposed control mechanism modulates the spanwise uniform boundary layer flow through the generation of spanwise-periodic streamwise-elongated streaks. Firstly, Direct Numerical Simulations (DNS) are performed at Mach 6 with imposed spanwise non-uniform wall temperature variations. The wall-prescribed temperature conditions successfully generate streaks of sufficient amplitude that suppress transitional instabilities. Secondly, a wind tunnel experimental implementation of a steady-state temperature control strategy is proposed, incorporating local cooling below a wall of finite thickness. A thermal model is employed to predict the surface temperature with varying cooling temperature inputs below the finite-thickness wall at Mach 6. The thermal analysis validates the methodology to achieve spanwise non-uniform temperature distributions and emphasizes the sensitivity of temperature input and wall-thickness variations. Lastly, to guide future experimental ground tests, restricted to short test duration, prototype models are proposed in order to regulate the local surface temperature by altering the local heat flux and diffusion within the wall. Specifically, dissimilar materials and thin wall structures are employed to achieve varying levels of heat diffusion and heat flux, respectively. Implementing these approaches generates a higher sensitivity to local surface temperature, thus enabling the passive achievement of spanwise non-uniform temperature distributions, as demonstrated by the thermal model.

Nomenclature

As_u^*	=	streak amplitude
A_v^*	=	amplitude of disturbances

^{*}Ph.D. Candidate, Department of Aeronautics.

[†]Ph.D. Candidate, Department of Aeronautics.

[‡]Senior Lecturer, Department of Aeronautics.

[§]Reader, Department of Aeronautics, AIAA Associate Fellow.

C_p	=	specific heat, J/(kg·K)
c	=	speed of sounds, m/s
D	=	length of simulation domain
E	=	perturbation energy, J
e_0	=	total energy, J
F	=	non-dimensional input frequency
f	=	Input frequency, Hz
h	=	local convection coefficient, W/(m ² ·K)
h_0	=	total enthalpy, J
L_{ref}	=	reference length, m
Ma	=	Mach number
N_E	=	N -factor
Pr	=	Prandtl number
p	=	pressure, Pa
$\mathbf{q}$	=	heat flux vector, W/m ²
Re	=	Reynolds number
Re_x	=	local Reynolds number
$Re_{x,model}$	=	local Reynolds number of thermal model
St_x	=	local Stanton number
T	=	temperature, K
T_r	=	recovery temperature, K
T_{sin}	=	spanwise varying wall temperature, K
t	=	time, s
t_{model}	=	wall thickness of thermal model, m
$t_{insulator}$	=	wall thickness of insulator, m
t_p	=	time scale of heat penetration, s
U_∞	=	freestream velocity, m/s
$\mathbf{u}$	=	velocity vector, m/s
V_h	=	indicator of heat transfer, m ³
x, y, z	=	spatial coordinates, m
α	=	thermal conductivity, W/(m·K)
γ	=	specific heat ratio

κ	=	thermal diffusivity m^2/s
λ	=	spanwise wavelength normalized by L_{ref}
μ	=	dynamic viscosity, $\text{kg}/(\text{s} \cdot \text{m})$
Θ	=	non-dimensional temperature, T/T_∞
ρ	=	density, kg/m^3
τ	=	stress tensor, $\text{kg}/(\text{s}^2 \cdot \text{m})$
ω	=	non-dimensional frequency of disturbance

Subscripts

∞	=	freestream quantity
BSL, BSH	=	backside low and high temperature
$high, low$	=	high and low quantity
$mean$	=	mean quantity
$model$	=	thermal model
ref	=	reference quantity
$start, end$	=	start and end location
$spin, spout$	=	sponge region at inlet and outlet

Superscript

$*$	=	non-dimensional quantity
$'$	=	perturbed/fluctuated quantity

I. Introduction

CONTROLLING laminar-to-turbulent boundary layer transition in hypersonic flow is crucial for developing hypersonic vehicles, significantly affecting vehicle performance. Boundary layer transition increases surface skin friction and aerodynamic heating, shaping these vehicles' overall performance [1]. Therefore, effectively controlling this transition in hypersonic flows is critical for pushing vehicle performance boundaries.

Laminar boundary layers can undergo diverse transition paths encompassing multiple instability mechanisms [2–4]. The second mode, Mack mode, emerges as the dominant instability at low freestream disturbance conditions in hypersonic flow [5]. The second mode features the growth of planar acoustic waves, which can be observed as 'rope-like' structure [6–8]. Upon reaching a specific amplitude, these linear instabilities initiate nonlinear interactions. Finally, these nonlinear interactions give rise to strong vertical waves, causing breakdown of the second mode and the onset of the transition process[9–11]. Previous studies have indicated that stationary velocity streaks can delay the breakdown of the second mode [12–14]. The optimal streak imposed by numerical amplitude can decrease the N -factor associated with

second Mack mode compared to a flow without streaks [13, 14]. Velocity streaks modify the instability characteristics of the second Mack mode, contributing to transition delay. While a strong streak can decrease the N -factor for second Mack mode, it can also destabilize the oblique sinuous first mode. However, an optimal amplitude streak can still delay transition onset by stabilizing both the first and second modes.

Actuation techniques are essential for generating streaks. Roughness elements have been studied as a potential method to generate streaks [1, 15–17]. The height and spacing of roughness elements significantly influence transition. Evidence points to the spacing between elements as a critical parameter for determining transition location; optimal spacing can promote streak generation, thereby suppressing transition [18]. However, the size and span of individual elements are highly sensitive parameters for the dimensions and placement of streaks, directly impacting transition control performance.

In other approaches, Vortex Generators (VGs) have been investigated to generate streaks [19, 20]. A single array of VGs has been shown to generate streaks strong enough to affect transition, and VG shape can be optimized to maximize the transition delay. However, these studies have shown that VGs can only suppress transition in limited conditions due to the sensitivity of their shape for generating streaks. Importantly, the transition control techniques mentioned above utilize convex geometries to forcibly alter the flow trajectory in order to generate streaks. Thus, the shape of these convex geometries must be optimized for each condition. In other words, at off-design conditions, these methods result in additional drag and heating for the vehicle and may even trigger earlier transition [21–23]. Therefore, it is desirable to develop alternative methods to generate streaks robustly and consistently across a range of operating conditions and delay transition.

This paper proposes a novel flow control concept to utilize spanwise non-uniform surface temperature distributions to induce streaks, thereby delaying transition in hypersonic flows. A similar approach has already been explored in low-speed flow: Fuaad et al.[24] and Hickey et al.[25] controlled transition in subsonic flow by introducing streaks through an array of hot and cold strips in the spanwise direction. These strips generated streaks induced by thermal effects caused by different local heat fluxes. This study extends the approach to the scenario of a high-speed boundary layer over a flat plate. In this paper, this method’s capability is explored and the actuation methods are designed for generating streaks. Our proposed method involves applying different wall temperatures to induce heat fluxes to the boundary layer, generating streaks, as illustrated in Fig. 1. Temperature distributions are achieved at each strip installed in a spanwise direction, altering density and viscosity in the boundary layer above each strip. This different spanwise thermal condition leads to stationary streaks, which are known to delay transition following the principles elucidated by Ren et al. [12] and Paredes et al. [13, 14].

The following sections detail Direct Numerical Simulation (DNS) investigations (Sec. II) to validate and explore this approach. Next, a preliminary study of the actuation method at a steady state is presented (Sec. III), followed by conceptual designs of candidate test articles for ground testing (Sec. IV). Thermal modeling of these conceptual designs

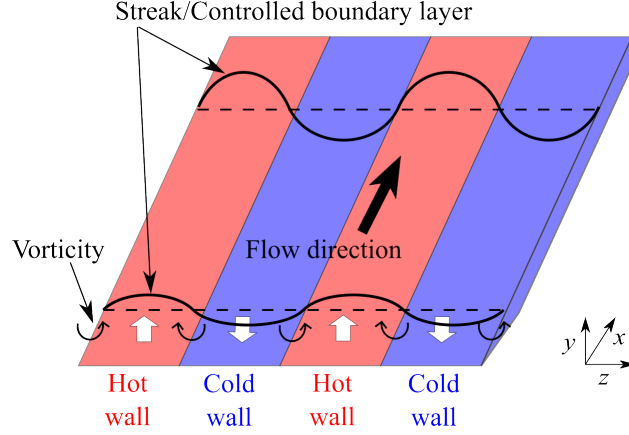


Fig. 1 Schematic of a wall-bounded flow controlled by spanwise non-uniform surface temperature. The generated streaks modulate the boundary layer in the spanwise direction to delay transition.

is performed to evaluate thermal performance under flight and ground test conditions. Finally, the concluding section (Sec.V) summarizes the essential findings and outlines directions for future research.

II. Direct Numerical Simulations at Mach 6

To prove the concept of the flow control method, DNS has been performed to characterize the generation of streaks. The velocity streak amplitude for various spanwise wavelengths of temperature distributions is quantified. The ability of streaks to suppress instabilities is also examined. A high-order finite-difference numerical code for hypersonic flow is used, the details of which can be found in Nagarajan et al.[26], Marxen et al.[27] and Margaritis et al.[28].

A. Governing equations and properties

In this study, DNS is employed to solve the discrete equations governing hypersonic flow. Space discretization is implemented by fourth-order compact finite differences [26, 28–30]. The time integration is carried out by an explicit third-order Runge-Kutta scheme. The non-dimensional governing equations used in the code are continuity, momentum, and energy equations in three-dimensional Cartesian coordinates,

$$\frac{\partial \rho^*}{\partial t^*} + \nabla \cdot (\rho^* \mathbf{u}^*) = 0, \quad (1)$$

$$\frac{\partial \rho^* \mathbf{u}^*}{\partial t^*} + \nabla \cdot (\rho^* \mathbf{u}^* \otimes \mathbf{u}^*) = -\nabla p^* + \nabla \cdot \boldsymbol{\tau}^*, \quad (2)$$

$$\frac{\partial \rho^* e_0^*}{\partial t^*} + \nabla \cdot (\rho^* h_0^* \mathbf{u}^*) = \nabla \cdot (\boldsymbol{\tau}^* \cdot \mathbf{u}^*) - \nabla \cdot \mathbf{q}^*, \quad (3)$$

where ρ^* , t^* , $\mathbf{u}^*$, p^* , $\boldsymbol{\tau}^*$, e_0^* , h_0^* , $\mathbf{q}^*$ are non-dimensional quantities for the density, time, velocity vector, pressure, stress tensor, total energy, total enthalpy, and the heat flux vector, respectively.

In the governing equations, non-dimensional quantities are defined with corresponding reference parameters as

$$\begin{aligned} T^* &= \frac{T}{T_{ref}}, \quad \rho^* = \frac{\rho}{\rho_\infty}, \quad p^* = \frac{p}{p_\infty}, \quad e_0^* = \frac{e_0}{c_\infty^2}, \\ h_0^* &= \frac{h_0}{c_\infty^2}, \quad \mu^* = \frac{\mu}{\mu_\infty}, \quad \mathbf{u}^* = \frac{\mathbf{u}}{c_\infty}, \end{aligned} \quad (4)$$

where c is the speed of sound and T_{ref} is defined as $T_{ref} = (\gamma_\infty - 1)T_\infty$, where γ_∞ is the specific heat ratio. Other dimensional reference parameters in Eq. 4 are selected as free stream parameters with subscript ∞ .

The Reynolds number Re , Mach number Ma and Prandtl number Pr are defined as

$$Re = \frac{\rho_\infty c_\infty L_{ref}}{\mu_\infty}, \quad Ma = \frac{U_\infty}{c_\infty}, \quad Pr = \frac{\mu_\infty C_{p\infty}}{\alpha_\infty}, \quad (5)$$

where U_∞ is the free stream streamwise velocity and L_{ref} is the reference length. The space coordinates and length properties are non-dimensionalized by L_{ref} , such as $x^* = x/L_{ref}$. The local Reynolds number Re_x is defined as

$$Re_x = \sqrt{x^*} Re = \sqrt{\frac{\rho_\infty c_\infty x}{\mu_\infty}}. \quad (6)$$

B. Computational domain and boundary conditions

The computational domain used to simulate the flow over the flat plate is shown in Fig. 2. A Cartesian coordinate system is used, where x^* is streamwise direction, y^* is wall-normal direction and z^* is spanwise direction. The domain consists of $1001 \times 211 \times 13$ points in x^* , y^* and z^* , respectively. The spanwise wavelength is defined as $\lambda_z^* = z_{domain}/L_{ref}$, and this also corresponds to the streak wavelength. The mesh resolution was established through past convergence studies [28], ensuring adequate spatial discretization to fully capture the flow dynamics. Within the second Mack mode planar wave's growth and decay region, the mesh incorporates approximately 120 grid points throughout the boundary layer (y^* -direction) and 13 points per wavelength in the streamwise (x^* -direction) to accurately resolve these structures. The number of mesh points in the z^* direction remains constant across different λ_z^* cases. The simulation domain starts at non-dimensional position $x_{start}^* = 14$ and ends at $x_{end}^* = 97$. The length of the sponge region at inlet is $L_{spin}^* = 5$ and at outlet is $L_{spout}^* = 15$.

A no-slip boundary condition is enforced at the wall for velocity and freestream conditions are used for the inflow, outflow, and top boundaries. An isothermal Dirichlet boundary condition is used for the wall temperature. The spanwise non-uniform surface temperature distributions are prescribed from the inflow boundary, where a compressible self-similar laminar solution based on the local wall temperature is imposed. The main simulation parameters are given in Tab. 1, which correspond to free flight in the International Standard Atmosphere at 20 km. To study the influence of spanwise non-uniform temperature distributions, temperature variations along the spanwise direction are introduced to

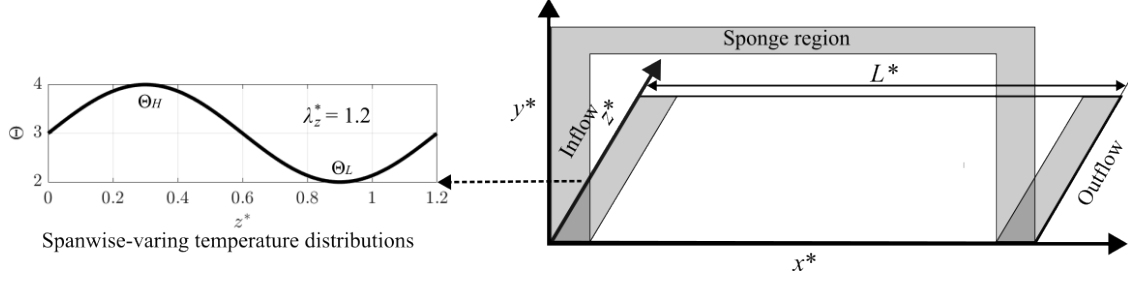


Fig. 2 Schematic of the DNS computational domain of the boundary layer flow over a flat plate with spanwise varying surface temperature.

Table 1 DNS parameters.

T_∞ (K)	p_∞ (Pa)	Ma	Pr	Re	L_{ref} (mm)	γ	Θ_L	Θ_H
216.7	5475.0	6.0	0.71	1.0×10^5	9.1	1.4	2	4

the boundary condition shown in Fig. 2.

A non-dimensional high wall temperature $\Theta_H = T_{high}/T_\infty$ and a non-dimensional low wall temperature $\Theta_L = T_{low}/T_\infty$ are brought into the sinusoidal function (Eq. 7) to obtain the spanwise varying wall temperature $\Theta_{sin}(z^*)$, which is then assigned to the boundary conditions,

$$\Theta_{sin}(z^*) = \frac{1}{2}(\Theta_H + \Theta_L) + \frac{1}{2}(\Theta_H - \Theta_L) \sin(2\pi z^*/\lambda_z^*). \quad (7)$$

$\Theta_L = 2$ and $\Theta_H = 4$ are set at the cool and hot regions, respectively.

C. DNS results

In this section, DNS results are presented that confirm the generation of streaks due to spanwise non-uniform surface temperature distribution, followed by a study of the (second Mack mode) instability growth with spanwise uniform and non-uniform temperature distributions.

1. Generation of streaks due to spanwise non-uniform surface temperature

A contour of the non-dimensionalized surface temperature Θ and isosurfaces of the streamwise velocity fluctuation $u^{*'} = u^* - u_{mean}^*$ are shown in Fig. 3a to highlight the flow modification due to the wall temperature variation, where u_{mean}^* denotes the spanwise- and time-averaged velocity u^* . A streaky structure can be identified as the streamwise velocity varies along the z^* direction. Low-velocity flow is located at the high-temperature region since low-momentum fluid close to the wall is lifted up due to the thermal variation, leading to a streaky structure in the near-wall region.

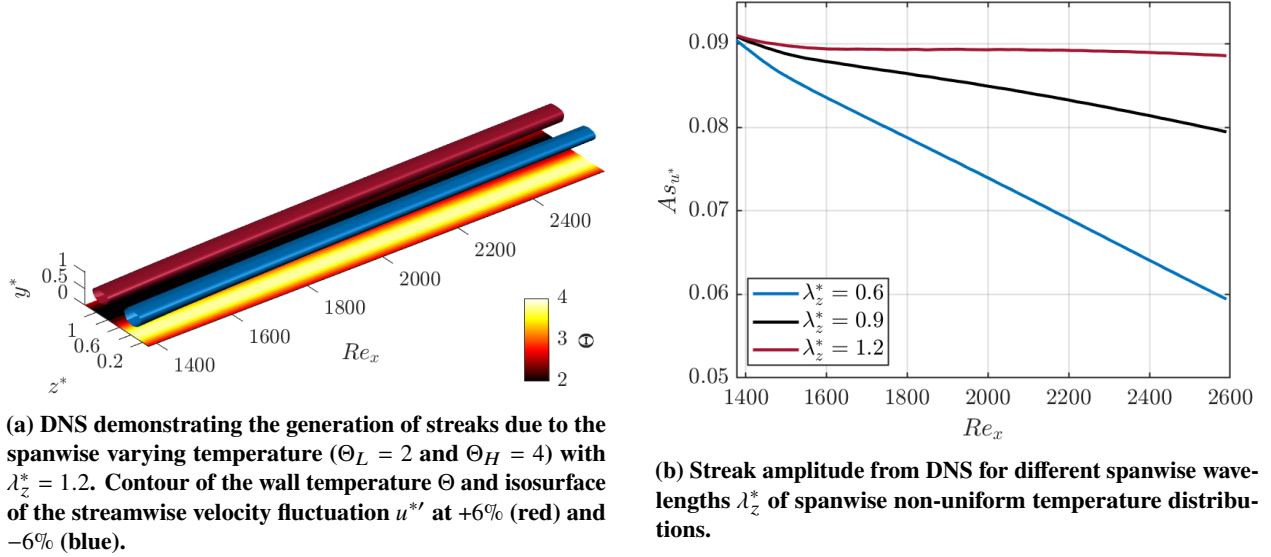


Fig. 3 Effects of spanwise varying temperature on flow structures and streak amplitude

The streak amplitude is calculated as

$$As_{u^*}(x^*) = \left[\max_{y^*, z^*}(u^*(x^*, y^*, z^*)) - \min_{y^*, z^*}(u^*(x^*, y^*, z^*)) \right] / 2 \quad (8)$$

and results are shown in Fig. 3b for several cases with different spanwise wavelengths $\lambda_z^* = 0.6, 0.9$ and 1.2 .

In Fig. 3b, after the sponge region $Re_x = 1378$, all cases show a streak amplitude of $As_{u^*} = 0.09$, as the inflow boundary layer conditions are calculated from a self-similar solution corresponding to the local wall domain temperature. The streak amplitude As_{u^*} of all the cases drops along the streamwise direction; however, with a larger λ_z^* , it maintains a higher level within the simulation domain. The influence of spanwise wavelength indicates that when the cold and hot strips are close to each other, the streak amplitude decreases faster, likely due to viscous effects. In contrast, when the wavelength increases, the thermal effect, which is the driving force of the streaks, becomes dominant so that the streak persists for a longer distance with large amplitude. Wavelengths larger than $\lambda_z^* = 1.20$ have not been explored due to high computation cost.

2. Instability suppression

In order to assess the instability growth, three DNS cases with disturbances have been conducted to verify the ability of temperature variations to suppress instabilities. In Case 1, the wall temperature is held constant at $\Theta_{L,H} = 2$, while in Case 2, it is maintained at $\Theta_{L,H} = 4$. In Case 3, the wall temperature varies in the spanwise direction between $\Theta_L = 2$ and $\Theta_H = 4$. The parameters related to instability triggering are listed in Tab. 2, where x_s^* and x_e^* are the locations of disturbance start and end, respectively. A_{v^*} is the amplitude of disturbances. ω is the non-dimensional frequency of

Table 2 DNS cases with external disturbance parameters to assess the effect of spanwise-varying temperature. The parameters of the blowing and suction actuator that trigger the second Mack mode are also given.

Case name	Description	Θ_L	Θ_H	λ_z^*	x_s^*	x_e^*	ω	A_{v^*}
Case 1	Constant wall temperature ($\Theta(z^*) = \text{const.}$)	2	2	∞	19.3	20.7	45	$0.001Ma$
Case 2	Constant wall temperature ($\Theta(z^*) = \text{const.}$)	4	4	∞	19.3	20.7	45	$0.001Ma$
Case 3	Variations of wall temperature ($\Theta(z^*) = f(z^*)$)	2	4	1.2	19.3	20.7	45	$0.001Ma$

disturbances as defined as the product of F , Ma and Re :

$$\omega = F Ma^2 Re, \quad (9)$$

$$F = 2\pi f^* (\mu_\infty^* / (\rho_\infty^* U_\infty^{*2})) \quad (10)$$

where F and f^* are the non-dimensional input frequency to DNS and the dimensional frequency, respectively. The choice of parameters is consistent with those expected to trigger the second mode in hypersonic flow [13]. The values of λ_z^* are ∞ for both Case 1 and Case 2 due to the uniform temperature wall, indicating the absence of a defined wavelength. On the other hand, $\lambda_z^* = 1.2$ is chosen for Case 3 to maintain a larger streak amplitude downstream, as shown in Fig. 3b.

The perturbation energy $E(x^*)$ is investigated by comparing results from the cases with and without streaks. $E(x^*)$ includes kinetic and potential perturbation energies based on the velocity, pressure, and temperature fluctuations, respectively [31]:

$$E(x^*) = \frac{1}{2} \int_{\Omega} \left[\overline{\rho^*} (u^{*2} + v^{*2} + w^{*2}) + \frac{\overline{T^*}}{Ma^2 \gamma \overline{\rho^*}} \rho^{*2} + \frac{\overline{\rho^*}}{\gamma(\gamma-1) Ma^2 \overline{T^*}} T^{*2} \right] d\Omega \quad (11)$$

where $\overline{\rho^*}$ and $\overline{T^*}$ present time-averaged values, and Ω denotes the $y^* - z^*$ cross-section of the simulation domain at location x^* . To evaluate the perturbation energy growth, a time-averaged N -factor N_E is evaluated, defined as

$$N_E(x^*) = \frac{1}{2} \left\langle \ln \frac{E(x^*)}{E(x_e^*)} \right\rangle_t, \quad (12)$$

where $\langle \rangle_t$ operates time averaging over 20 samples taken within one period of the imposed instability frequency ($F = 7.5 \times 10^{-5}$). Following time averaging, spatial averaging is conducted in the spanwise direction.

Figure 4 shows the pressure perturbation field in the $x^* - y^*$ plane, illustrating disturbance growth that triggers the second Mack mode for Case 1, Case 2 and Case 3 (cold and hot sides). In Case 1 and 2, clear amplification of perturbations and typical signature of second Mack mode are observed, consistent with the previous DNS studies under similar conditions [32, 33]. Relative to Case 2, Case 1 exhibits a more pronounced growth in perturbations as expected due to the lower surface temperature, which is known to destabilize the second Mack mode [5]. In Case 3, both the cold

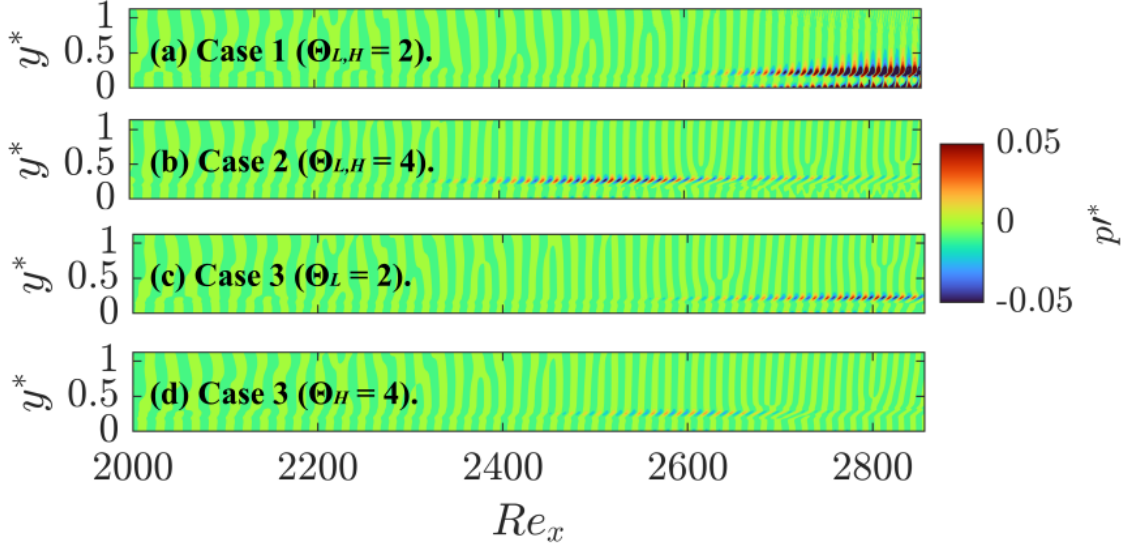


Fig. 4 Instantaneous pressure perturbation field for Case 1, Case 2, Case 3 (cold side) and Case 3 (hot side) on the $x^* - y^*$ plane.

and hot sides exhibit the classical amplification and decay of the second Mack mode. However, the pressure fluctuation amplitude in Case 3 is significantly lower than in Cases 1 and 2. This indicates that the instability in Case 3 is effectively attenuated downstream.

Figure 5 displays N -factor against Re_x for all three cases. The Case 1, which is a uniform temperature $\Theta_{L,H} = 2$ without streaks, shows the highest disturbance amplitude, peaking around $N_E = 5$ close to the end of the domain around $Re_x = 2800$. On the other hand, in the Case 2 ($\Theta_{L,H} = 4$) the N -factor peaks earlier at $Re_x = 2500$ with N_E below 4. These observations are consistent with the peaks in Fig. 4 and published literature on the features of the second Mack mode [5]. In the Case 3, as shown by the spatial-averaged curve, with streaks generated by the non-uniform wall temperature, the peak of N_E is significantly reduced compared to both the Case 1 and Case 2. Additionally, the N_E in the Case 3 is separately evaluated only in the hot and cold wall regions, as “Case 3 hot side” and “Case 3 cold side” in Fig. 5, respectively. In Case 3, the N_E on the hot side exhibits a delayed onset of amplification compared to Case 2 (constant hot wall temperature), with two peaks appearing around $Re_x = 2600$ and 2800 . Conversely, on the cold side in Case 3, the amplification of the N_E begins earlier than in Case 1 (constant cold wall temperature). However, the rate of increase on the cold side in Case 3 is slower than in Case 1, and the amplification remains below the peak observed in Case 1 after $Re_x = 2600$. These results demonstrate that, in Case 3, both the spatially averaged and hot-cold separated analyzes indicate significant suppression of instability due to the streaks generated by the spanwise temperature variations. The initial temperature distribution—and consequently, the local Reynolds number—is expected to influence streak growth, thereby affecting instability development. Poulain et al. investigated the effects of streamwise wall temperature variations on hypersonic boundary layer transition, demonstrating the sensitivity of instability growth

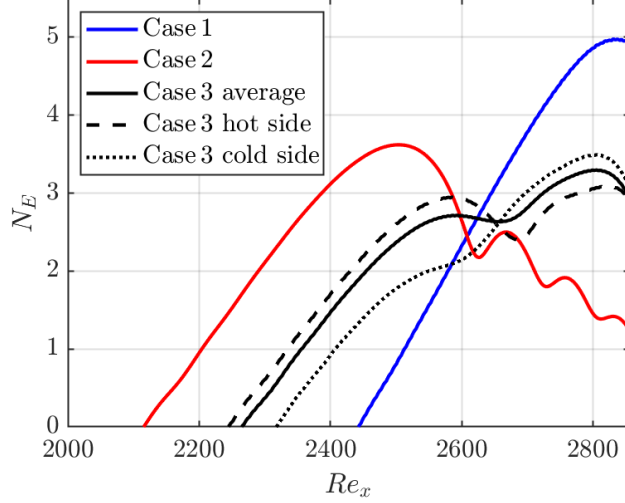


Fig. 5 N -factors of the Case 1 (uniform cold wall), the Case 2 (uniform hot wall), and the Case 3 (spanwise-varying wall temperature). For the Case 3, also the N -factor is separately evaluated only in the hot (“hot side”) and cold wall (“cold side”) regions, respectively.

to localized heating and cooling [34]. While this study focuses on streak generation through spanwise non-uniform surface temperature distributions, the sensitivity to variations in the streamwise location of the control methods requires further investigation.

III. Control methodology and performance under flight conditions

This section evaluates the control methodology required to achieve spanwise non-uniform surface temperature distributions by examining the heat transfer between a high-speed boundary layer and a solid surface. The influence of the parameters of wall thickness and wall materials is explored in controlling the local surface temperature. Control effectiveness is characterized using different materials such as aluminum, titanium, and MACOR.

A. Principles of three-dimensional steady-state condition

To achieve the desired steady-state temperature distributions under hypersonic cruising conditions, an actuation method is essential, such as an internal cooling system, to regulate the surface temperature distributions and mitigate aerodynamic heating from the boundary layer. As the temperature of the boundary layer increases due to viscous effects, heat transfer to the vehicle’s surface occurs based on the temperature gradient. Consequently, the cooling system plays a critical role by continuously absorbing the heat. This absorption maintains equilibrium with the incoming heat, thereby ensuring the desired temperature distributions are steadily achieved.

The basic design of a cooling system is explored through the three-dimensional steady state heat transfer equation, defined as:

$$\frac{d^2T}{dx^2} + \frac{d^2T}{dy^2} + \frac{d^2T}{dz^2} = 0, \quad (13)$$

where T and x, y , and z represent temperature and the three-dimensional axis, respectively. Figure 6 illustrates the wall temperature gradient, accounting for convection from the boundary layer and a cooling system input. The heat flux on the vehicle surface varies locally due to boundary layer conditions, with aerodynamic heating being more intense upstream as a result of a higher convection coefficient. This spatially varying aerodynamic heating induces three-dimensional heat diffusion within the wall. Thus, designing the desired surface temperature distribution requires a 3D model that incorporates these effects.

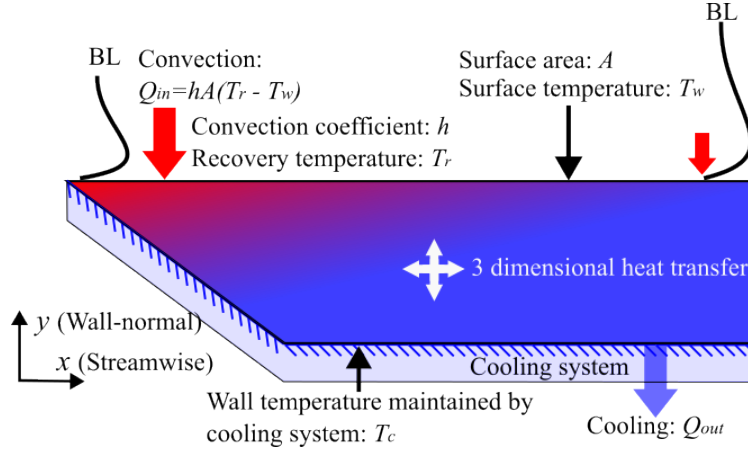


Fig. 6 Schematic of the steady state wall condition under aerodynamic heating by hypersonic cruising with a cooling system.

B. Thermal modeling for three-dimensional steady-state condition

This subsection studies the control actuation methodology to generate non-uniform surface temperature distributions under a three-dimensional wall at steady state. Thermal modeling is developed to approximate surface temperature distributions. The performance of surface temperature distributions is evaluated with various cooling temperatures, wall thicknesses, and materials.

1. Heat transfer coefficient

An accurate estimation of convection between the boundary layer and the test surface is a pivotal step in approximating thermal designs. A 3D steady state heat transfer equation is solved. At aerodynamic test surface, the convective coefficient h , the recovery temperature T_r , and the wall surface temperature T_w are estimated and set as the boundary conditions [35, 36]. T_r can be considered constant in both the streamwise and spanwise directions along the surface. The local convection coefficient h is assumed as a function of the local Stanton number St_x , which depends on the local Reynolds number $Re_{x,model}$ and the Prandtl number Pr , employing the Chilton-Colburn analogy [37]. For a laminar

boundary layer on a flat plate, the local Stanton number St_x can be approximated as

$$St_x = \frac{0.332}{\sqrt{Re_{x,model}}} Pr^{-0.67}, \quad (14)$$

$$Re_{x,model} = x Re_{model} = \frac{\rho_{ref} U_{\infty} x}{\mu_{ref}}. \quad (15)$$

Here, the unit Reynolds number Re_{model} is calculated as 6.49×10^5 /m at Mach 6, matching the DNS conditions of the previous section with $Pr = 0.71$. ρ_{ref} and μ_{ref} are the reference density and the reference viscosity, respectively, utilizing the reference temperature based on the Meador-Smart method. The local convection coefficient can then be estimated using the expression

$$h = St_x (\rho_{ref} U_{\infty} C_p) \quad (16)$$

where U_{∞} represents the freestream velocity and $C_p = 1010 \text{ J/(kgK)}$ denotes the specific heat at constant pressure. Additionally, viscosity is determined using Sutherland's law considering the reference temperature. Density and viscosity are evaluated based on reference temperatures to account for conditions near the wall.

2. Thermal model and geometry

A parabolic differential equation solver available in MATLAB's Partial Differential Equation Toolbox is employed to calculate heat transfer. Based on the DNS parameters in Tab. 1, the spanwise wavelength is defined as $\lambda_z^* = 1.2$, corresponding to a physical length of $z = 10.9$ mm. As a result, the hot and cold strips each have a width of 5.45 mm. Our fundamental geometry is defined by a surface with length of 250 mm and a sharp 12° leading edge. The thermal modeling parameters are the wall thickness and the cooling temperature imposed on the wall.

A sketch of the geometry is depicted in Fig.7. The geometry consists of cooling inputs to implement hot and cold strips, respectively. The hot strip is sandwiched between the half-width cold strips to simulate periodic conditions. On the aerodynamic test surface, a convective heat flux is imposed, based on Eq. 16, considering T_r of 1531 K, consistent with the DNS conditions listed in Tab. 1. Each backside surface maintains a uniform cooling temperature, defined by Θ normalized by T_{∞} . Behind the cold strips, a stronger cooling wall, denoted as Θ_{BSL} , is set. A weaker cooling wall, denoted as Θ_{BSH} , is defined behind the hot strip. The DNS defines a spanwise pair of strips to generate the cold and hot wall with $\Theta_L = 2$ and $\Theta_H = 4$ to generate the spanwise non-uniform surface temperature distributions. Thus, Θ_{BSL} and Θ_{BSH} are selected as combinations of $\Theta_{BSL,BSH} = [2, 4]$ and $[1, 5]$. Other surfaces are defined as adiabatic walls. Aluminum is chosen as the wall material and the wall thickness t_{model} is varied between 2.5 and 10 mm, outlined in Tab. 3. The 3D geometry model utilizes an unstructured mesh, and a mesh resolution study is conducted, ultimately settling on a resolution of 0.85 mm.

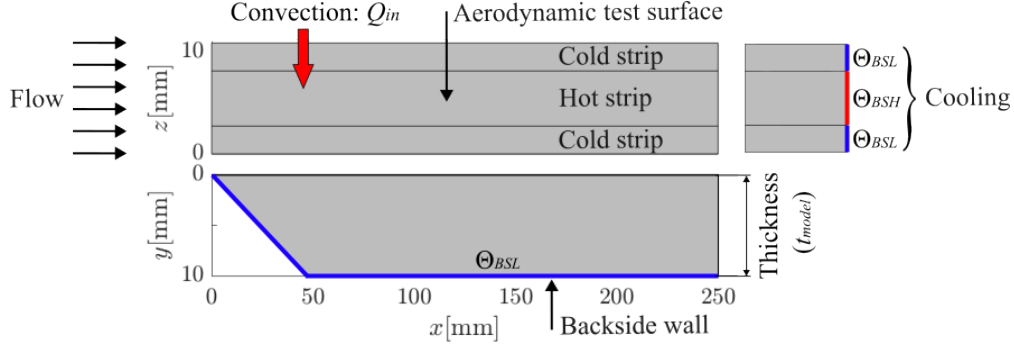


Fig. 7 Schematic of the thermal modeling domain, including the cold and hot strips. The domain shows the convection Q_{in} at the aerodynamic test surface and the stronger (Θ_{BSL}) and the weaker (Θ_{BSH}) cooling at each strip.

Table 3 Aluminum wall test cases with varying wall thickness and stronger Θ_{BSL} and weaker Θ_{BSH} cooling temperatures inputs behind the wall.

Name (Material - t_{model} - Θ_{BSL} - Θ_{BSH})	t_{model} [mm]	Θ_{BSL}	Θ_{BSH}
AL-2.5-2-4	2.5	2	4
AL-5-2-4	5	2	4
AL-10-2-4	10	2	4
AL-2.5-1-5	2.5	1	5
AL-5-1-5	5	1	5
AL-10-1-5	10	1	5

C. Spanwise surface temperature distributions

1. Performance with uniform wall thicknesses and differential cooling under an aluminum wall

The steady-state non-dimensional surface temperature profiles with $\Theta_{BSL} = 2$ and $\Theta_{BSH} = 4$ are depicted in Figs. 8a-c. Spanwise temperature differences can be observed in all cases. Overall, the leading edge shows both the highest temperature on the central hot region and the lowest temperature on the outer cold region due to the cooling extending until the leading edge. After the leading edge, each strip temperature sustains a certain temperature depending on t_{model} , shown in Fig. 9a. Specifically, for AL-2.5-2-4 (Fig. 8a), pronounced temperature differences obviously appear between the hot and cold regional surfaces. Fig. 9b presents the temperature distribution in the spanwise direction at $x = 150$ mm. The temperature variations show the temperature peak at the center of each strip and the continuous distribution between peaks. In contrast, AL-10-2-4 exhibits an almost uniform temperature surface, as shown in Figs. 8c, 9a and 9b. AL-5-2-4 shows slight temperature variations (Fig. 8b).

Figs. 8d-f illustrate cases with a higher imposed temperature gradient ($\Theta_{BSL} = 1$ and $\Theta_{BSH} = 5$). Comparison of Figs. 8d-f with 8a-c reveals that the temperature distributions remain similar. However, the hot and cold surfaces attain higher and lower temperatures, respectively, shown in Figs. 8d and 8e. AL-10-1-5, with the thickest wall of 10 mm, exhibits a less noticeable temperature variation similar to that of AL-10-2-4 (Fig. 8c) due to the dominant effect of

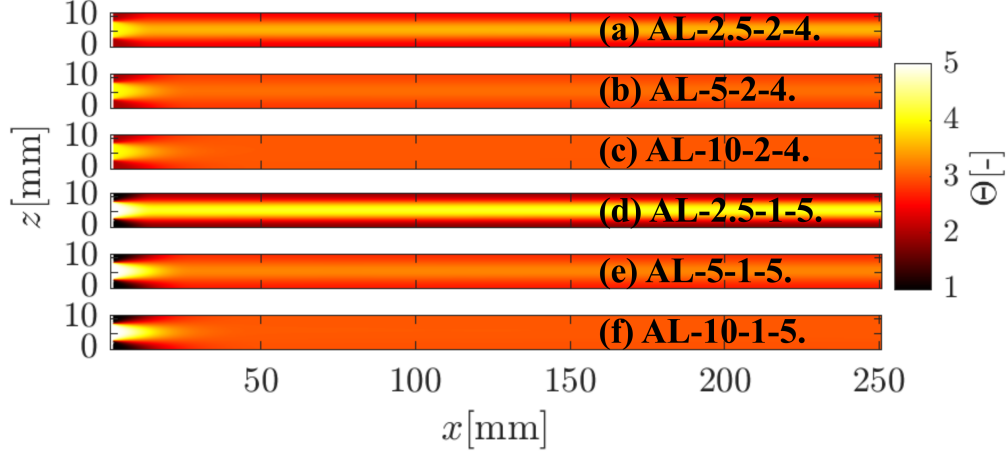


Fig. 8 Surface normalized temperature at the steady state under aluminum wall with $\Theta_{BSL} = 2$ and $\Theta_{BSH} = 4$ (a to c) and $\Theta_{BSL} = 1$ and $\Theta_{BSH} = 5$ (d to f).

Table 4 Different wall material cases for sensitivity study.

Name (Material - t_{model} - Θ_{BSL} - Θ_{BSH})	Material	t_{model} [mm]	Θ_{BSL}	Θ_{BSH}
T-5-2-4	Titanium	5	2	4
T-10-2-4	Titanium	10	2	4
M-5-2-4	MACOR	5	2	4
M-10-2-4	MACOR	10	2	4

thermal diffusion in the wall.

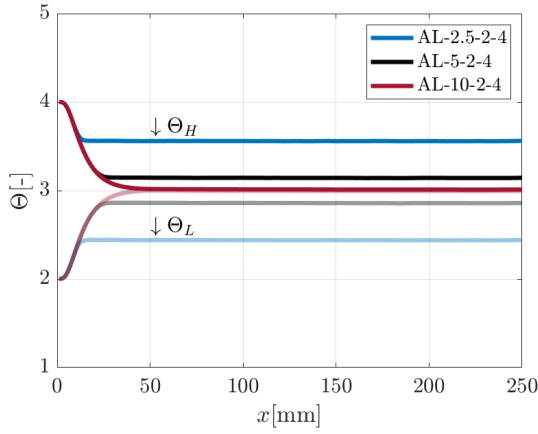
2. Temperature distribution dependence on material

This subsection delves into the sensitivities of surface temperature distributions to different materials. Titanium and ceramics (MACOR) are selected for their temperature response, with titanium having 20 times lower thermal conductivity than aluminum, and ceramics (MACOR) 10 times lower than titanium. Table 4 outlines four test cases, maintaining consistent geometry and mesh conditions from previous cases. Thicknesses of 5 and 10 mm are chosen, with temperature inputs of $\Theta_{BSL} = 2$ and $\Theta_{BSH} = 4$.

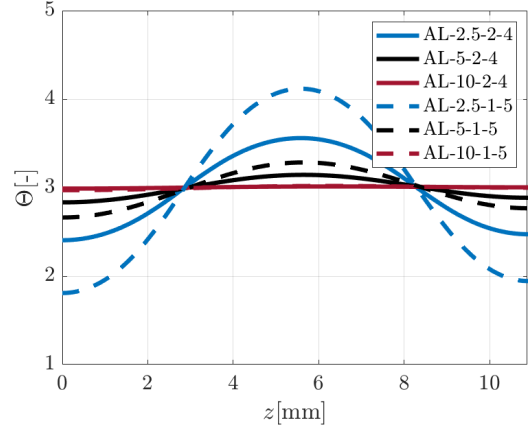
Fig. 10 presents the surface temperature distributions for all cases. T-5-2-4 and T-10-2-4 exhibit slight temperature differences similar to AL-5-2-4 and T-10-2-4. M-5-2-4 shows more temperature differences than titanium. Conversely, M-10-2-4 exhibits high surface temperatures with less temperature difference. These results demonstrate that the surface temperature varies less over the strips using materials with lower thermal conductivities.

D. Discussion

Fig. 11 illustrates the surface temperature on the hot and cold strips' centers at 150 mm along aluminum, titanium, and MACOR with 2.5, 5 and 10 mm wall thickness. All materials show similar behavior. With $t_{model} = 2.5$ mm, high and low temperatures are observed, while the $t_{model} = 10$ mm cases show minimal temperature differences. One



(a) Streamwise normalized temperature (Solid and light lines are at the strips' centerline on the hot Θ_H and cold Θ_L regions, respectively).



(b) Spanwise normalized temperature with $\Theta_{BSL} = 1$ and $\Theta_{BSH} = 5$ (dash line) at $x = 150$ mm.

Fig. 9 Streamwise and spanwise normalized temperature in different aluminum wall thickness cases.

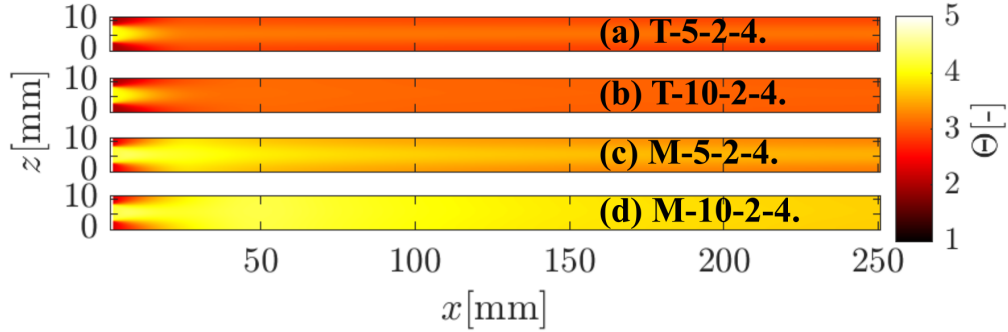


Fig. 10 Surface normalized temperature for T-5-2-4, T-10-2-4, M-5-2-4 and M-10-2-4 with $\Theta_{BSL} = 2$ and $\Theta_{BSH} = 4$.

exception is for $t_{model} = 10$ mm, where thick MACOR achieves higher temperatures than thin MACOR since low thermal diffusion decreases the cooling effect and increases the surface temperature. Overall, metals easily sustain temperature differences caused by cooling input due to their high conductivity. Moreover, the combination of parameters such as wall thickness, cooling temperatures, and material choice is key to generating complex temperature distributions.

IV. Passive control approach for ground test implementation

In this section, a strategy to generate spanwise non-uniform surface temperature distributions in short-duration wind tunnel tests is explored. The passive control method is introduced, along with control parameters to achieve surface temperature variations. Four conceptual designs are considered and their performance is evaluated using thermal modeling. The performance of the insulator is characterized by its material properties and thickness.

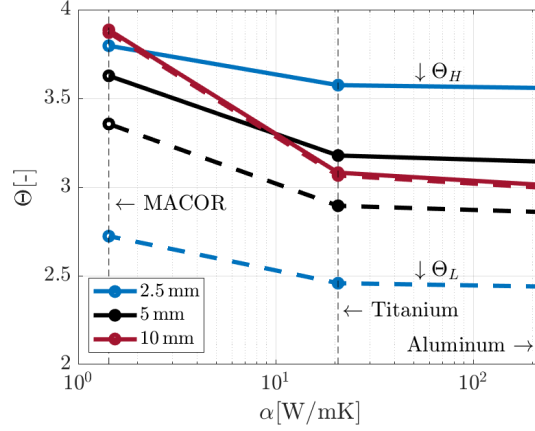


Fig. 11 Variation of temperature difference with $\Theta_{BSL} = 2$ and $\Theta_{BSH} = 4$ on each strip at $x = 150$ mm with varying materials (MACOR, titanium, and aluminum) and wall thickness of 2.5, 5 and 10 mm. Solid lines represent the hot, and dash lines represent the cold strips.

A. Principles of passive transient temperature control in short wind tunnel operation

Practical testing in wind tunnels is crucial for validating the flow control methodology using spanwise non-uniform surface temperature distributions. However, achieving a steady state presents a significant challenge within the constraints of short-duration experiments or insufficient incoming enthalpy for wind tunnel conditions. Rather, it is necessary to assess performance based on instantaneous states of the time-dependent surface temperature evolution during a wind tunnel test. The transient wall temperature can be described using Fourier's law of heat transfer:

$$\begin{aligned} \frac{\partial T}{\partial t} &= \frac{\alpha}{\rho C p} \nabla^2 T \\ &= \kappa \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right), \end{aligned} \quad (17)$$

where T , t , α , ρ , Cp , κ , x , y , and z represent temperature, time, thermal conductivity, density, specific heat, thermal diffusivity, and spatial directions, respectively. To reach steady-state conditions, steady-state temperature distributions dominated by material properties must converge. Since the system's response speed is also governed by material properties, reaching the desired surface temperature distribution through active control takes time, making the process challenging in short-duration wind tunnel tests. Conversely, passive control methods can simplify the system by using heat flux from the flow to passively increase the local wall temperature. While the surface temperature may not reach steady-state conditions, the instantaneous flow field can still be evaluated under transient wall surface temperature conditions. Therefore, a passive control method is chosen to demonstrate spanwise non-uniform temperature distributions.

High-enthalpy tunnels can reach high heat fluxes. This high heat flux allows control of the local heating process and achieves hot and cold strips as long as the initial wall temperature is lower than the recovery temperature. While heat

transfer occurs during a wind tunnel run, alternating material properties κ and spatial temperature distribution $\nabla^2 T$ act as key parameters to passively achieve different temperatures on the hot and cold strips simultaneously, as explained in Eq. 17. For alternating material properties, the local material of the wall can be modified to change local temperature sensitivity. Conversely, altering the spatial temperature distributions, achieved by changing the local inner wall structure to control heat flux diffusing through the wall, can generate different local temperatures, as demonstrated in Sec III.C.1. In the subsequent section, conceptual flat plate models are preliminarily designed based on the alternating material approach and spatial temperature distributions.

B. Conceptual design

Three conceptual flat plate models are presented in this section to passively achieve non-uniform temperature distributions during the transient state that exists in wind tunnel operation. The mechanism of achieving different temperatures and practicality is discussed.

1. Spanwise arrangement of two dissimilar materials

The dissimilar materials approach achieves non-uniform surface temperatures during high-enthalpy wind tunnel tests, where the surface is exposed to high heat flux, depicted in Fig. 12b, with the targeted wall temperature distributions and assumed boundary conditions shown in Fig. 12a. Two materials with dissimilar thermal properties are periodically combined in the spanwise direction on the surface: one material with low thermal diffusivity acting as an insulator strip and another with high thermal diffusivity functioning as a thermal conductor strip. This different thermal diffusivity allows the generation of different transient surface temperatures during high heat flux conditions.

The lower diffusivity materials for the insulator strip facilitate slower heat diffusion. As a result, under exposure to high heat flux, the surface temperature in the insulator strips increases, rapidly approaching the recovery temperature. In contrast, materials with higher diffusivity play a role in maintaining material temperature, as their high heat diffusion capabilities help distribute heat quickly. Figure 13a visualizes multiple materials' thermal conductivities and diffusivities. Within Fig. 13a, it is essential to note that metals and graphite exhibit the highest thermal diffusivity, followed by ceramics, composites, foams, and polymers, each successively exhibiting lower diffusivity levels. Various materials are selected based on manufacturing availability and explored for their diffusivity properties as temperature increases, exemplified in Fig. 13b. Copper and aluminum are exceptional conductors, with copper offering superior performance due to its higher density and greater thermal capacity. Titanium, titanium alloys, and Inconel 625, have significantly lower diffusivity which are suitable for the insulator material. Graphite(G348) is versatile, potentially acting as either the insulator or conductor depending on the operation temperature. MACOR (ceramics) has the lowest diffusivity among all materials, making it highly preferable as an insulator.

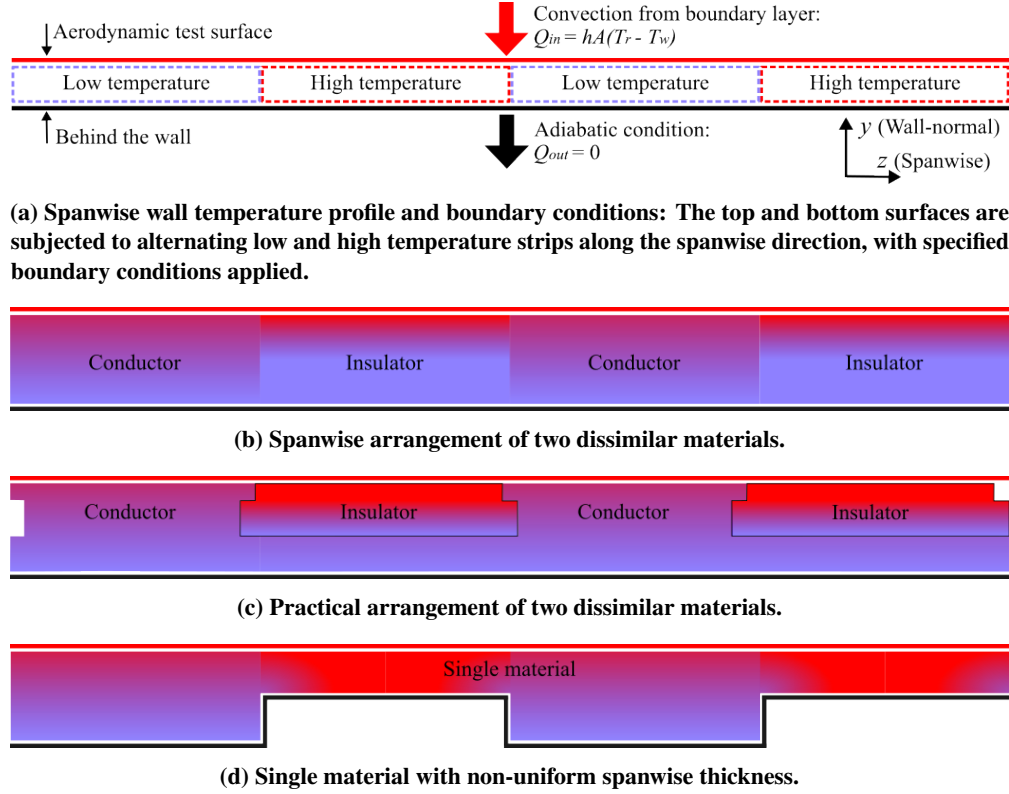
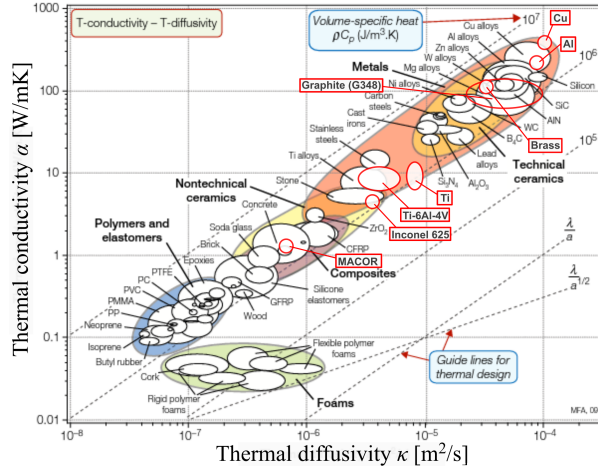


Fig. 12 Sketches of the wall temperature profile and boundary conditions (a) and the three conceptual designs for achieving spanwise temperature difference approached by varying the materials (b) and (c), and thickness of the wall (d).

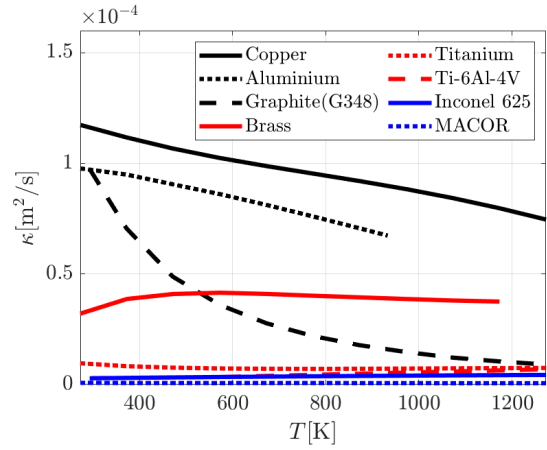
2. Practical arrangement of two dissimilar materials

The spanwise arrangement of two dissimilar materials aims to exploit varying thermal properties to generate different surface temperatures. However, this leads to practical challenges in combining materials with differing thermal expansion properties to achieve the desired wall temperature. For instance, handling the considerable tensile and shear stresses that occur on bolts can be challenging when dealing with materials that expand at different rates. High-temperature adhesives, commonly used for bonding, may also introduce uncertainties when applied in similar conditions. Therefore, exploring alternative bonding methods that avoid reliance on bolts or adhesives is recommended. As a practical solution, thin strip-shaped insulators are embedded in an otherwise solid block of the conducting base material.

A preferred approach involves having the materials hold their positions relative to each other through the use of geometric shapes achieved by machining. This is illustrated in Fig. 12c, where tooth-shaped slots are cut into the base material. In essence, these tooth-shaped slots serve as receptacles for the insulator strips. This method of employing toothed slots has the advantage that these grooves securely hold the insulator strip in place, even when materials with different thermal expansion coefficients are involved. While there is a risk of small gaps forming due to differential



(a) Thermal conductivity plotted against thermal diffusivity [38], highlighting the selected materials property.



(b) Thermal diffusivity changes with temperature with the selected materials.

Fig. 13 Thermal properties across materials and their temperature sensitivity.

thermal expansion, this is mitigated by the relatively small dimensions of the slots. This approach mitigates the risk of cracks or structural issues that might arise during the assembly process using screws or other traditional fasteners. In cases where the expansion of the insulator material is larger than the conductor material, roles can be reversed; tooth-shaped conductors can be slotted into the insulator instead.

3. Single material with non-uniform spanwise thickness

A single material with a non-uniform spanwise thickness generates surface temperature distributions by controlling heat flux through variations in the inner wall structure, as illustrated in Fig. 12d. In thin walls, owing to limitation of heat flux through the small cross-sectional area, the surface temperature increases rapidly. With a cold wall initial condition, in the beginning, uniform heat transfer via convection takes place at the same streamwise location. Once both walls begin to heat, the temperature of the thinner wall rises more rapidly than that of the thicker wall, due to the restricted heat flux passing through its smaller cross-sectional area. The spanwise non-uniform temperature distribution appears until both walls reach the same temperature. This approach is advantageous when the entire rig needs to be made from a single type of material and/or single manufacturing method.

C. Thermal modeling for transient surface temperature

The heat transfer is approximated using the same estimation method as described in Sec. III.B. The freestream condition is consistent with Sec. III.B.2. The geometries of the conceptual models are depicted in Fig. 14. Figures 14a and 14b illustrate the spanwise arrangement of two dissimilar materials and the practical arrangement of two dissimilar materials, respectively. In both cases, the central material acts as an insulator sandwiched between conductors to simulate periodic conditions. The insulator thickness of the practical arrangement is chosen to be 4 mm for manufacturing

Table 5 Materials selection for the conceptual models in the thermal modeling.

Name	Approach	Insulator	Conductor
M-C (Base)	The spanwise arrangement of two dissimilar materials	MACOR	Copper
M-C (Practical)	The practical arrangement of two dissimilar materials	MACOR	Copper
T-A	The practical arrangement of two dissimilar materials	Titanium	Aluminum
M-M (1 mm)	The single material with non-uniform spanwise thickness	MACOR (1 mm)	MACOR
M-M (4 mm)	The single material with non-uniform spanwise thickness	MACOR (4 mm)	MACOR
A-A (1 mm)	The single material with non-uniform spanwise thickness	Aluminum (1 mm)	Aluminum
T-T (1 mm)	The single material with non-uniform spanwise thickness	Titanium (1 mm)	Titanium

simplicity. The heat transfer in the model includes lateral heat transfer to account for the interaction between the dissimilar materials. Figure 14c displays the single material with non-uniform spanwise thickness where the central part serves as an insulator sandwiched by conductors. The central thickness is the parameter for the insulator.

All test cases selected for each approach and materials are outlined in Tab. 5. For the spanwise arrangement of two dissimilar materials and their practical arrangement, a machinable ceramic (MACOR) is selected as the insulator for its low thermal diffusivity, as illustrated in Fig. 13b. Conversely, copper, boasting high thermal diffusivity, is designated as the conductor. The case of M-C (Base) and M-C (Practical) is the spanwise arrangement of two dissimilar materials and their practical arrangement, respectively. In order to assess the insulator's sensitivity, titanium and aluminum are selected as an additional case (T-A) based on the practical arrangement configuration, these are for the insulator and conductor, respectively. To evaluate the sensitivity of the insulators, titanium and aluminum are selected as the add-on case (T-A) for the insulator and conductor, respectively, based on the practical arrangement configuration. For the single material with non-uniform spanwise thickness approach, MACOR is chosen due to the lowest thermal diffusivity, which causes temperature gradients quickly. The insulation thicknesses of $t_{insulator} = 1$ mm (M-M (1 mm)) and 4 mm (M-M (4 mm)) are chosen. Additionally, A-A and T-T are investigated, using 1mm insulator thickness employing aluminum and titanium, respectively.

The 3D models are designed using CAD software before the data is imported into MATLAB. For the 3D geometry model, a resolution study of the unstructured mesh is conducted, ultimately settling on a test surface and base resolution of 2.5 and 5 mm, respectively. A time step of 0.1 s is chosen, and the transient thermal analysis spans a duration of 15 s, mirroring a typical wind tunnel experimental time frame. Regarding boundary conditions, except for the test surface, all other surfaces are defined as adiabatic walls. The initial temperature across the entire plate is set at 300 K.

D. Prediction of the transient temperature

The surface temperature distributions of all cases based on the three conceptual models at $t = 15$ s are represented in Fig. 15. The spanwise temperature profile on the center of the strips after $t = 15$ s is presented in Fig. 16a. In all cases, the temperature is high at the leading edge due to the highest convection and thinner wall. After the leading

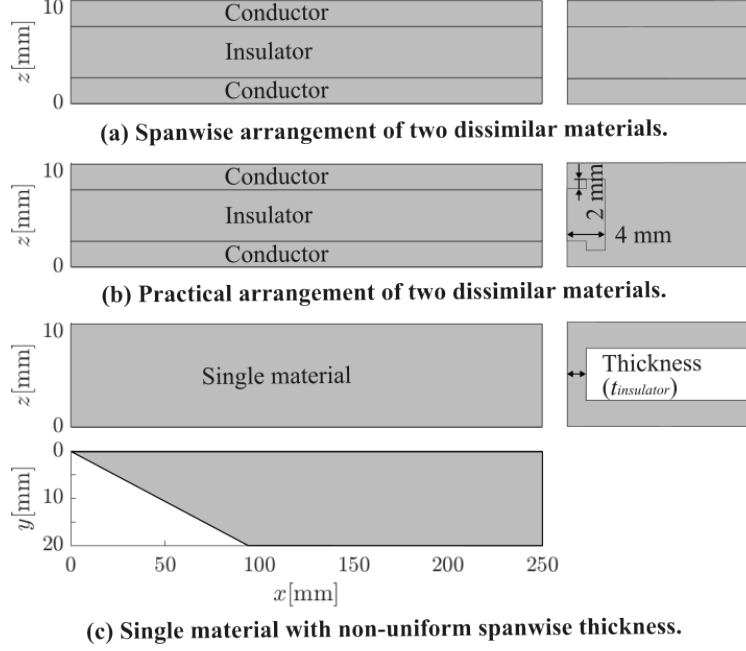


Fig. 14 The geometries of three conceptual designs in the thermal modeling.

edge, both cases with the M-C (Figs. 15a and 15b) exhibit apparent temperature differences between the insulator Θ_H and conductor Θ_L . After $x = 100$ mm, the changes in Θ_H and Θ_L become smaller, respectively. Along the spanwise direction, there is a smooth and monotonic temperature increase towards the center to of the insulating strip. Peak temperatures are observed at the center of the insulating strip, as illustrated in the spanwise temperature profiles plotted in Fig. 16a. Meanwhile, the conductor maintains a uniform temperature due to its higher thermal diffusivity. The spanwise temperature profiles confirm that the practical arrangement of two dissimilar materials yields results similar to the spanwise arrangement approach. The temperature difference between the peak temperature of the insulator and conductor reaches 0.37 in both cases at $x = 150$ mm, proving the possibility of generating spanwise non-uniform temperature distributions. On the other hand, the T-A case shows uniform temperature across the spanwise direction. This indicates that the thermal diffusivity of titanium is not sufficient, which is ten times higher than that of MACOR.

Temperature distributions of the single material of non-uniform spanwise thickness cases are depicted in Figs. 15d-g. The M-M (1 mm) shows apparent temperature distributions, while the M-M (4 mm) has barely any temperature distributions in both the spanwise direction shown in Fig. 16a. The conductor temperature distribution of the M-M (1 mm) shows a more gradual temperature gradient than the dissimilar material approach across the spanwise direction. The thinner wall of the insulator demonstrates a sensitivity to temperature changes. At the A-A, the surface temperature shows a uniform distribution. Meanwhile, the T-T shows a slight temperature difference between the insulator and conductor. The insulator temperature at the T-T is higher than that at the T-A, suggesting that the thinner titanium insulator can generate higher temperatures. To generate higher temperature differences, both approaches of two

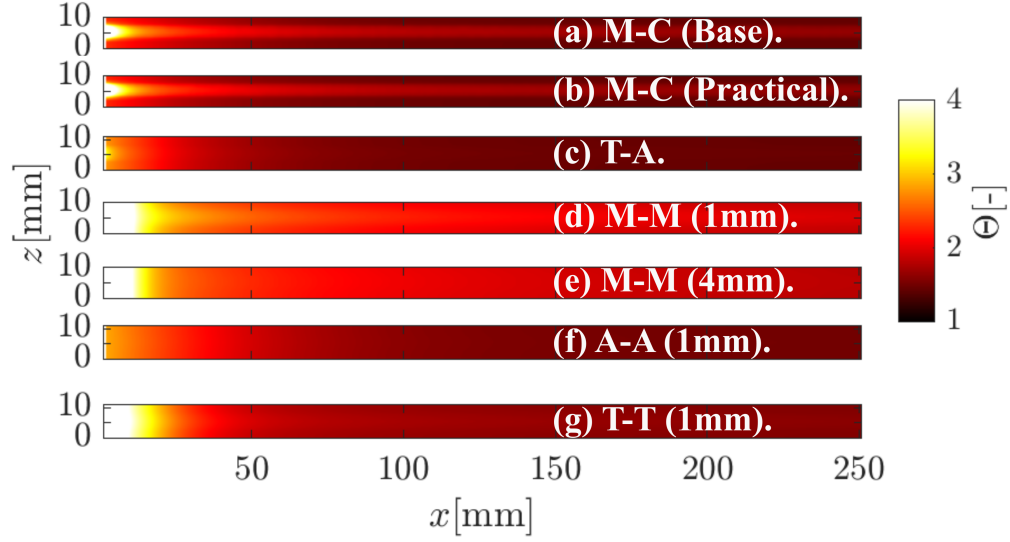
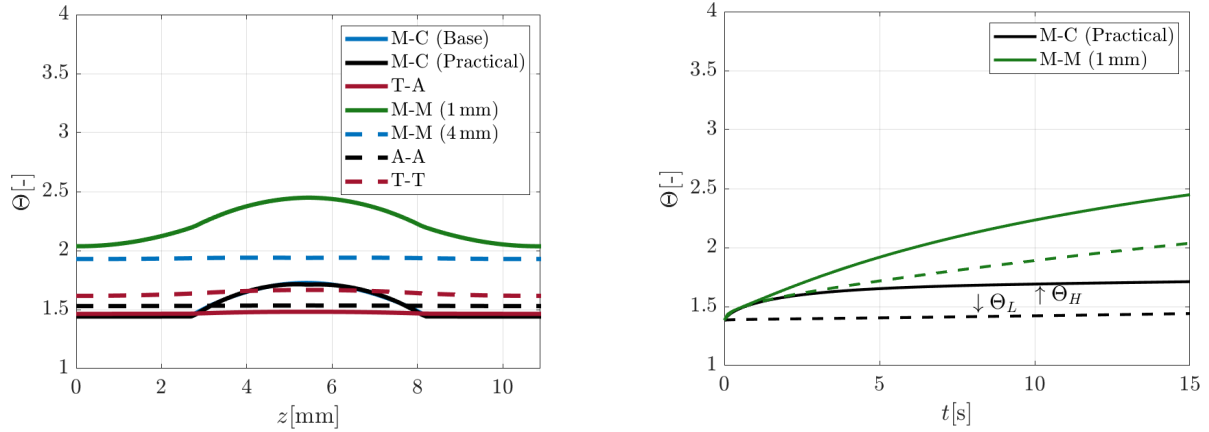


Fig. 15 Surface normalized temperature of the conceptual designs calculated by the thermal modeling.

dissimilar materials and the single material with non-uniform spanwise thickness require less thermal diffusivity material or a thinner insulator.



(a) Spanwise normalized temperature profile at the aerodynamic test surface at $x = 150$ mm at $t = 15$ s.

(b) Transient normalized temperature at the strips' center at $x = 150$ mm along exposure time (Solid and dash lines are the insulator and conductor, respectively).

Fig. 16 Surface normalized temperature plot along the spanwise direction and transient normalized temperature at $x = 150$ mm.

Fig. 16b illustrates the temperature evolution over time at the strip's center ($x = 150$ mm) for the M-C (Practical) and M-M (1 mm), both selected for their higher temperature distributions. Notably, both cases exhibit distinct temperature differences from the outset, maintaining a consistent difference throughout the exposure time. The M-C (Practical) causes the temperature difference to be maintained without further heating, while the M-M (1 mm) gradually attains a higher and higher temperature difference as time increases. In the M-C (Practical), a temperature difference is

initially established, after which the temperatures of both the insulator and conductor remain constant, maintaining the temperature difference. Conversely, in the M-M (1 mm), the temperature difference gradually increases over time after $t = 2$ s. These results highlight the importance of the insulator geometry in achieving significant temperature differences.

E. Discussion

1. Impact on performance of insulator material selection

The insulators utilized in both the practical arrangement of dissimilar materials and the structural heat flux approaches are selected based on their low diffusivity properties and/or thinness, aimed at increasing local temperatures to achieve significant temperature differences compared to the conductors. Table 6 presents the thermal diffusivity ratio between the insulator and conductor, along with the temperature difference ($\Delta\Theta$) at $t = 15$ s for the materials employed in the practical arrangement cases. Notably, $\Delta\Theta$ is 16 times higher with MACOR and copper than titanium and aluminum, while the $\kappa_{conductor}/\kappa_{insulator}$ ratio is 24 times higher.

Table 6 Thermal diffusivity ratio of the insulator and conductor materials.

Insulator, Conductor	MACOR, Copper	Titanium, Aluminum
$\kappa_{conductor}/\kappa_{insulator}$	261	11
$\Delta\Theta_{t=15}$	0.38	0.024

2. Characterization of insulator's thickness

Results presented in this paper have shown that the thickness of the insulator $t_{insulator}$ is a crucial parameter that significantly affects temperature distributions in cases with a single non-uniform thickness material, along with material thermal diffusivity κ . Figure 17 shows the temperature at the center of a thin strip at $x = 150$ mm at $t = 15$ s plotted against the product of κ and $t_{insulator}$ for a range of the MACOR and titanium test cases. Two trends are evident from Fig. 17: (1) low values of $\kappa t_{insulator}$ produce high wall temperatures for both the thick and thin parts of the wall, and (2) low values of $\kappa t_{insulator}$ lead to an increase in the temperature difference between the thick and thin regions of the wall. The parameter $\kappa t_{insulator}$ (with units of volumetric flow rate) effectively characterizes the efficiency at which a configuration is able to transfer heat between the thin and thick parts of the wall structure and thus inhibit or promote the volumetric flow of thermal energy (i.e. heat transfer) between the thick and thin parts. In both MACOR and titanium, it is evident that as $\kappa t_{insulator}$ decreases, i.e., the wall becomes thinner, the local temperature of the insulator increases linearly on a logarithmic scale. Given MACOR's lower $t_{insulator}$, it achieves the highest local temperature with the thinnest wall. On the other hand, Θ_L effectively sustains the equilibrium temperature due to a relatively large wall thickness. In terms of $\Delta\Theta$, the reduction in $\kappa t_{insulator}$ exhibits a more apparent impact across different materials, indicating the critical role of insulator thickness in achieving temperature differences.

This result suggests that using a low diffusivity material with a thin profile can increase the temperature on the

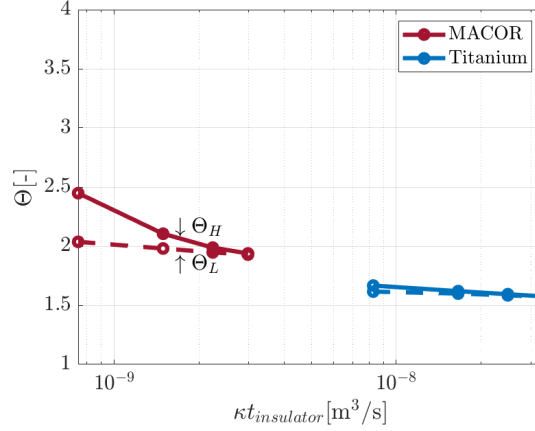


Fig. 17 Normalized temperature at the strips' center surface at $x = 150$ mm at $t = 15$ s for MACOR and titanium multiplied by thermal diffusivity and the insulator thickness ($t_{insulator} = [1, 2, 3, 4]$ mm).

insulator surface, while the opposite properties are optimal for the conductor to maintain surface temperature. To achieve even greater temperature differences, adjustments such as altering the incident flow angle or freestream pressure to enhance convection or implementing additional heating on the insulator and cooling on the conductor may be necessary.

V. Conclusion

A novel flow control concept has been presented to delay boundary layer transition in high-speed flows by employing spanwise non-uniform surface temperature distributions. The proposed approach involves implementing hot and cold strips along the spanwise direction, inducing different thermal boundary conditions. This results in velocity gradients of flow in the spanwise direction within the boundary layer, introducing streaks. The presence of these streaks effectively inhibits the transition process.

The underlying principles of this concept are elucidated and validated through the DNS of a Mach 6 boundary layer, incorporating spanwise non-uniform wall temperature distributions. The resulting flow field exhibits the generation of streaks, showcasing the influence of wavelength on streak amplitude. Furthermore, the evaluation of perturbation energy illustrates the instability onset of transition, revealing attenuated peak and delayed growth.

A steady-state temperature control strategy is proposed for actuation methods to demonstrate the spanwise non-uniform surface temperature distributions. Behind a finite wall, differential cooling is imposed across the spanwise direction. Thermal modeling assesses the performance of the surface temperature distribution for an aluminum wall. Results indicate that a thinner wall or intense local cooling can exhibit apparent surface temperature variations. Titanium and MACOR (ceramics) are also explored to investigate the sensitivity of thermal conductivity. Both titanium and MACOR can also induce temperature differences, providing the wall is sufficiently thin.

Passive control approaches are explored for short-duration wind tunnel tests. To achieve non-uniform temperature

distributions within a brief experimental time frame, heat transfer is utilized to heat the local surface from the boundary layer passively. Three conceptual approaches are proposed. A spanwise arrangement of dissimilar materials approach utilizes materials with two extremely different thermal properties, MACOR and copper, for the insulator and the conductor, respectively. Due to the difference in the thermal diffusion speeds, the insulator rapidly increases the surface temperature while the conductor maintains the surface temperature. These different thermal properties allow the surface to achieve temperature differences. In order to implement this method in a wind tunnel, a practical arrangement is proposed using tooth slots to be assembled to overcome potential problems due to differential thermal expansion. This approach is able to achieve equivalent surface temperature variation performance to that of the spanwise arrangement of two dissimilar materials approach. A single material approach with non-uniform spanwise thickness is also proposed to achieve surface temperature distributions by modifying the wall structure. Local regions of low wall thickness limit internal heat transfer, resulting in increased local temperatures. The insulator's temperature is characterized by the ratio of thermal diffusivity between the insulator and conductor and the insulator's thickness.

References

- [1] Schneider, S. P., "Effects of roughness on hypersonic boundary-layer transition," *Journal of spacecraft and rockets*, Vol. 45, No. 2, 2008, pp. 193–209.
- [2] Reshotko, E., "Boundary-layer stability and transition," *Annual review of fluid mechanics*, Vol. 8, No. 1, 1976, pp. 311–349.
- [3] Morkovin, M., "Transition in open flow systems-a reassessment," *Bull. Am. Phys. Soc.*, Vol. 39, 1994, p. 1882.
- [4] Fedorov, A., "Transition and stability of high-speed boundary layers," *Annual review of fluid mechanics*, Vol. 43, 2011, pp. 79–95.
- [5] Mack, L. M., "Boundary-layer linear stability theory," Tech. rep., California Inst of Tech Pasadena Jet Propulsion Lab, 1984.
- [6] Zhang, C., Zhu, Y., Chen, X., Yuan, H., Wu, J., Chen, S., Lee, C., and Gad-el Hak, M., "Transition in hypersonic boundary layers," *AIP Advances*, Vol. 5, No. 10, 2015, p. 107137.
- [7] Casper, K. M., Beresh, S. J., Henfling, J. F., Spillers, R. W., Pruett, B. O., and Schneider, S. P., "Hypersonic wind-tunnel measurements of boundary-layer transition on a slender cone," *AIAA Journal*, Vol. 54, No. 4, 2016, pp. 1250–1263.
- [8] Laurence, S., Wagner, A., and Hannemann, K., "Experimental study of second-mode instability growth and breakdown in a hypersonic boundary layer using high-speed schlieren visualization," *Journal of Fluid Mechanics*, Vol. 797, 2016, pp. 471–503.
- [9] Pruett, C., and Zang, T., "Direct numerical simulation of laminar breakdown in high-speed, axisymmetric boundary layers," *Theoretical and Computational Fluid Dynamics*, Vol. 3, No. 6, 1992, pp. 345–367.
- [10] Stetson, K., and Kimmel, R., "On the breakdown of a hypersonic laminar boundary layer," *31st Aerospace Sciences Meeting*, 1993, p. 896.

- [11] Pruett, C. D., and Chang, C.-L., "Spatial direct numerical simulation of high-speed boundary-layer flows Part II: Transition on a cone in Mach 8 flow," *Theoretical and Computational Fluid Dynamics*, Vol. 7, No. 5, 1995, pp. 397–424.
- [12] Ren, J., Fu, S., and Hanifi, A., "Stabilization of the hypersonic boundary layer by finite-amplitude streaks," *Physics of Fluids*, Vol. 28, No. 2, 2016, p. 024110.
- [13] Paredes, P., Choudhari, M. M., and Li, F., "Transition delay in hypersonic boundary layers via optimal perturbations," Tech. rep., NASA/TM-2916-219210, 2016.
- [14] Paredes, P., Choudhari, M. M., and Li, F., "Instability wave–streak interactions in a high Mach number boundary layer at flight conditions," *Journal of fluid mechanics*, Vol. 858, 2019, pp. 474–499.
- [15] James, C. S., "Boundary-layer transition on hollow cylinders in supersonic free flight as affected by Mach number and a screwthread type of surface roughness," Tech. rep., 1959.
- [16] Fujii, K., "Experiment of the two-dimensional roughness effect on hypersonic boundary-layer transition," *Journal of Spacecraft and Rockets*, Vol. 43, No. 4, 2006, pp. 731–738.
- [17] Fiala, A., Hillier, R., and Estruch-Samper, D., "Roughness-induced turbulent wedges in a hypersonic blunt-body boundary layer," *Journal of fluid mechanics*, Vol. 754, 2014, pp. 208–231.
- [18] Taylor, O., "Effects of Surface Roughness on Boundary Layer Transition in a Hypersonic Flow," Ph.D. thesis, Imperial College London, Imperial College London, Exhibition Road, London SW7 2AZ, 5 2019.
- [19] Paredes, P., Choudhari, M. M., and Li, F., "Transition delay via vortex generators in a hypersonic boundary layer at flight conditions," *2018 Fluid Dynamics Conference*, 2018, p. 3217.
- [20] Pederson, C., Paredes, P., Choudhari, M. M., Zhou, B. Y., and Diskin, B., "Shape Optimization of Vortex Generators to Control Mack Mode Amplification," *AIAA Aviation 2020 Forum*, 2020, p. 2963.
- [21] Holloway, P. F., and Sterrett, J. R., "Effect of controlled surface roughness on boundary-layer transition and heat transfer at mach numbers of 4.8 and 6.0," Tech. rep., 1964.
- [22] Choudhari, M., Li, F., and Edwards, J., "Stability analysis of roughness array wake in a high-speed boundary layer," *47th AIAA Aerospace Sciences Meeting including The New Horizons Forum and Aerospace Exposition*, 2009, p. 170.
- [23] Paredes, P., De Tullio, N., Sandham, N., and Theofilis, V., "Instability study of the wake behind a discrete roughness element in a hypersonic boundary-layer," *Instability and Control of Massively Separated Flows: Proceedings of the International Conference on Instability and Control of Massively Separated Flows, held in Prato, Italy, from 4-6 September 2013*, Springer, 2015, pp. 91–96.
- [24] Fuaad, P., Baig, M., and Khan, B., "Turbulent drag reduction using active control of buoyancy forces," *International Journal of Heat and Fluid Flow*, Vol. 61, 2016, pp. 585–598.

- [25] Hickey, J.-P., Younes, K., Yao, M. X., Fan, D., and Mouallem, J., “Targeted turbulent structure control in wall-bounded flows via localized heating,” *Physics of Fluids*, Vol. 32, No. 3, 2020, p. 035104.
- [26] Santhanam Nagarajan, Lele, S., and Ferziger, J., “A robust high-order compact method for large eddy simulation,” *Journal of Computational Physics*, Vol. 191, No. 2, 2003, pp. 392–419. Place: USA Publisher: Academic Press.
- [27] Marxen, O., Magin, T., Iaccarino, G., and Shaqfeh, E. S. G., “A high-order numerical method to study hypersonic boundary-layer instability including high-temperature gas effects,” *Physics of Fluids*, Vol. 23, No. 8, 2011. Publisher: American Institute of Physics Inc.
- [28] Margaritis, A. T., Scherding, C., Marxen, O., Schmid, P. J., and Sayadi, T., “Development of a high-fidelity computational tool for chemically reacting hypersonic flow simulations,” *arXiv preprint arXiv:2210.05547*, 2022.
- [29] Nagarajan, S., Lele, S., and Ferziger, J., “Leading-edge effects in bypass transition,” *J. Fluid Mech.*, Vol. 572, 2007, pp. 471–504.
- [30] Marxen, O., Iaccarino, G., and Shaqfeh, E. S., “Disturbance evolution in a mach 4.8 boundary layer with two-dimensional roughness-induced separation and shock,” *J. Fluid Mech.*, Vol. 648, 2010, pp. 435–469.
- [31] Chu, B.-T., “On the energy transfer to small disturbances in fluid flow (Part I),” *Acta Mechanica*, Vol. 1, No. 3, 1965, pp. 215–234.
- [32] Egorov, I., Fedorov, A., and Soudakov, V., “Direct numerical simulation of disturbances generated by periodic suction-blowing in a hypersonic boundary layer,” *Theoretical and Computational Fluid Dynamics*, Vol. 20, 2006, pp. 41–54.
- [33] Chuvakhov, P. V., and Fedorov, A. V., “Spontaneous radiation of sound by instability of a highly cooled hypersonic boundary layer,” *Journal of Fluid Mechanics*, Vol. 805, 2016, pp. 188–206.
- [34] Poulain, A., Content, C., Rigas, G., Garnier, E., and Sipp, D., “Adjoint-based linear sensitivity of a supersonic boundary layer to steady wall blowing–suction/heating–cooling,” *Journal of Fluid Mechanics*, Vol. 978, 2024, p. A16.
- [35] Gramola, M., Bruce, P. J., and Santer, M. J., “Temperature corrected Pressure Sensitive Paint measurements on a flexible surface,” *AIAA Scitech 2021 Forum*, 2021, p. 0128.
- [36] Gramola, M., Bruce, P. J., and Santer, M. J., “Engineering model for heat transfer to a complex-geometry deployable heat shield,” *AIAA Scitech 2021 Forum*, 2021, p. 0355.
- [37] White, F., and Corfield, I., *Viscous Fluid Flow*, vol. 3 McGraw-Hill, New York, 2006.
- [38] Ashby, M. F., *Materials selection in mechanical design*, fifth edition. ed., Butterworth-Heinemann, Amsterdam, 2017 - 2017.