

Article

Correction of Aero-Optical Effect with Blow–Suction Control for Hypersonic Vehicles

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Abstract: High-speed turbulence induces significant aero-optical effects that severely disrupt the functionality of imaging systems of hypersonic vehicles. In this study, the aero-optical correction of various jet cooling modes is investigated using a Terminal High Altitude Area Defense (THAAD)-like seeker model and the imaging impact of high-speed flow field and flow control on the optical window is analyzed by the Delayed Detached Eddy Simulation (DDES) method. The findings reveal that a jet mode parallel to the window exhibits better cooling effectiveness compared to a perpendicular jet mode along the body axis; however, it introduces additional wavefront distortion, leading to degraded imaging quality. Although micro-vortex generators (MVGs) can reduce density fluctuations near the window from a refractive index perspective, they do not effectively mitigate wavefront distortion or improve window cooling efficiency. Finally, incorporating suction control, a comprehensive flow control solution, significantly improves the flow field structure near the window, resulting in a more uniform temperature distribution and reduced wavefront distortion. Applying this flow control method results in a 14.7% reduction in wavefront distortion at 3 Ma and an approximately 20% maximum value reduction at 5 Ma. This study proposes a novel and comprehensive flow control method to effectively mitigate the aero-optical effect in hypersonic flows, providing a new avenue for subsequent researchers in this field.

Keywords: seeker head; aero-optical effect; flow control; cooling; micro-vortex generator



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1. Introduction

When a missile flies at high speed in the atmosphere, the optical guidance hood of the missile interacts strongly with the surrounding gas, resulting in the formation of turbulent boundary layers, shock waves, and expansion waves around the optical hood. These complex flow structures create density gradients in the flow field outside the missile hood. When the missile-borne imaging system captures and identifies targets while tracking them, light waves passing through this non-uniform refractive index gradient in the flow field cause optical distortion, leading to the migration, blurring, jittering, and energy attenuation of the target images. This phenomenon is known as aero-optical effect [1,2]. However, during high-speed flight, aero-thermal factors cause a rapid rise in temperature on the outer surface of the optical window of an optical seeker. This leads to a gradient distribution and may cause shape change or even structural damage of the optical window [3]. Therefore, it is crucial to develop effective cooling measures for hypersonic vehicles' optical windows to reduce aerodynamic thermal radiation effects and improve optical imaging.

In recent years, there has been increasing attention to aero-optical effects due to the development of airborne laser communication platforms, high-speed missiles with optical seekers, and laser weapon systems. The correction of aero-optical effects mainly focuses on flow control and image processing. Many scholars have made significant advancements in the correction of aero-optical effects within the mixing layer [4,5]. Xu et al. [6,7] proposed a backward ray-tracing method for the aero-optics simulation of a typical blunt-nosed vehicle,

which has improved the convenience of calculation. Guo et al. [8–10] conducted an extensive investigation into the temporal and spatial development of the supersonic mixing layer and proposed a correction strategy through flow control by periodic pulse forcing.

In 1966, Goldstein [11–13] provided a definitive elucidation of jet cooling, while also investigating the empirical formulation for gas film cooling efficiency and the impact of slit shape on gas film cooling. Wyckham and Smits [14] used a two-dimensional Shack–Hartmann wavefront sensor to study aero-optical distortion in turbulent boundary layers at transonic and hypersonic speeds, with and without gas injection. Banakh et al. [15] analyzed the characteristics of optical beams propagating through a shock wave resulting from supersonic motion of a conical body in a turbulent atmosphere. It was shown that the aero-optical effects caused by a shock wave are suppressed with an increase in optical turbulence. Guo et al. [16] used the ray tracing method to study the effects of different altitudes, attack angles, beam incidence angles, and Mach numbers on beam propagation in the study of aero-optics effects caused by the flow field around hypersonic vehicles in near-space. It was found that the aero-optical effects become more serious in the presence of jet compared to the absence of jet. Later, Fan et al. [17] investigated the influence of altitude on aero-optic imaging quality degradation of the hemispherical optical dome. The results showed that under the same flight conditions, with the increase in altitude, the peak signal-to-noise ratio (PSNR) of the distorted image and the SR result increased, and the radiuses of dispersion spots, including 80% energy, decreased. Lee et al. [18,19] investigated the aero-optics of the external jet cooling and shock wave in a shock-wave wind tunnel. Moreover, Xing [20] employed the DSMC method to investigate the impact of different jet modes on aero-optics-induced image migration, and the results demonstrated that the horizontal jet mode yields superior results in terms of window cooling. Experimental research conducted by Ding et al. [21] revealed that the utilization of micro-vortex generators has significantly contributed to enhancing wavefront stability.

To summarize, it is evident that previous studies have employed a relatively simplistic experimental method, with limited involvement of hypersonic speeds in numerical simulations of Mach numbers. In the context of aero-optical effects at higher Mach numbers, there is a dearth of numerical simulations with enhanced precision and more efficient jet cooling strategies. Furthermore, there is a lack of an effective measure to mitigate the additional wavefront distortion introduced by jet injection. To address this research gap, the present study employs the DDES method to accurately simulate the flow field. Additionally, a comprehensive blowing–suction flow control approach with MVGs is proposed for correcting aero-optical distortions, thereby effectively mitigating wavefront aberration. The proposed method improves the uniformity of temperature distribution across the optical window, effectively mitigating the impact of aerothermal effects and offering a novel solution for aero-optical correction.

The remainder of this paper is organized as follows: In Section 2.1, a seeker model similar to THAAD (Terminal High Altitude Area Defense) is proposed as the basis for all subsequent studies. In Section 2.2, a series of correction methods for aero-optical effects are presented. In Section 3, the evaluation parameters for aero-optical effects are introduced, followed by a presentation of the governing equations of the numerical method. In Sections 3.3 and 3.4, the numerical method validation and grid-independent analysis are carried out. Section 4 compares the influence of different jet modes on both window cooling and the formation of flow field structures near the window. The impact on aero-optical effect correction is then examined through comparison after incorporating MVGs. Furthermore, Section 4.3 investigates changes in refractive index within the flow field and explores improvements in imaging effects resulting from blow–suction control. Finally, the conclusions drawn from this paper are summarized in Section 5.

2. Physical Model and Correction Method

2.1. THAAD-like Seeker Model

As the aircraft's speed increases, the aerodynamic heat of the optical window also increases. Elevated temperatures can lead to two issues: firstly, they can result in significant thermal radiation noise, which affects the normal functioning of the detection system; secondly, they may cause cracks in the optical window due to thermal stress. Therefore, when designing a hypersonic infrared imaging seeker, it is essential to consider various design factors, such as the imaging guidance system, aero-optical effects, head-heat protection, infrared window and aerodynamic resistance. These design requirements should be combined effectively. To mitigate the pneumatic heating's impact on the optical window and reduce challenges in refrigeration system design, a cooling method utilizing side recess windows is employed to lower the temperature of the optical window.

The seeker's optical window retains a concave structure, similar to that of the THAAD model, as shown in Figure 1a, with a recessed height of $\delta = 4$ mm, due to airflow passage, and a slope length of 5.7 mm connecting its outer surface to the seeker wall. The dimensions for the optical window itself are 200 mm in length and 60 mm in width. Additional dimensions for other parts of the window model are illustrated in Figure 1b below. Positioned on top of the missile body, this optical window is located on its leeward side when flying at an angle of direct attack. Sapphire is selected as the optical window material, and its physical properties are shown in Table 1 below.

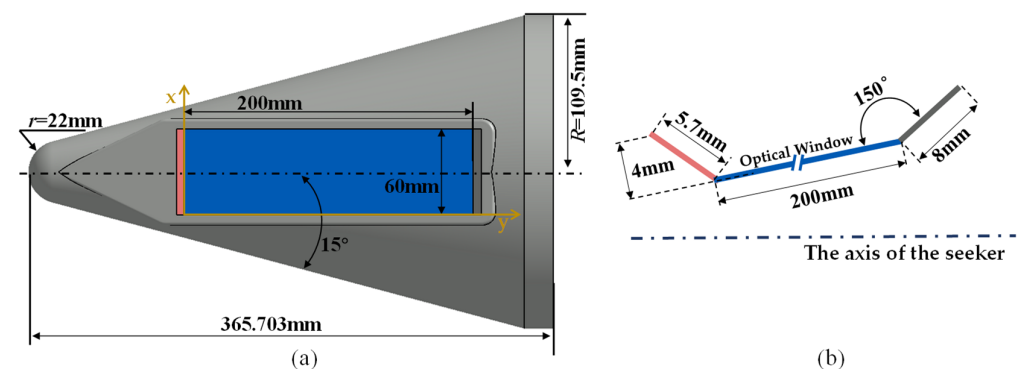


Figure 1. (a) THAAD-like seeker geometry parameters. (b) Geometric parameters near the optical window.

Table 1. Physical properties of optical window [22].

Density	Fusing Point	Elastic Modulus	Specific Heat Capacity	Thermal Conductivity	Yield Strength	Poisson's Ratio
3980 kg/m ³	2050 K	344 GPa	750 J/(kg·K)	36 W/(m·K)	300 MPa	0.27

2.2. Correction Method

In order to cool the optical seeker's window, two different jet cooling methods are employed. The low temperature jet separates the optical window from the high-temperature incoming flow, effectively cooling it. The cooling nozzle is positioned upstream of the window, at a slope angle of 15° with respect to the projectile body axis, as depicted in Figure 2 below.

The direction of the cooling jet is indicated by the red arrow. For selecting an appropriate jet medium, low-temperature nitrogen is chosen due to its easy availability and relatively low cost, with a jet temperature set to 77 K. The incoming flow conditions consist of air at Mach numbers of 3 and 5, a specific heat ratio of 1.4, and the wall temperature maintained at 300 K. Other detailed parameters, such as static pressure and static temperature can be found in Table 2.

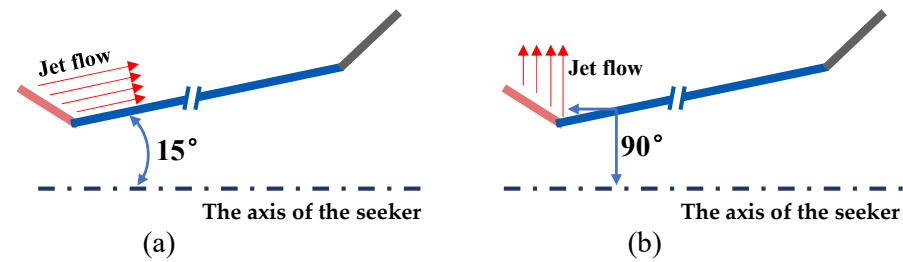


Figure 2. Schematic diagram of two jet-cooling modes: (a) $\theta = 15^\circ$ and (b) $\theta = 90^\circ$.

As previously mentioned, the key aspect in suppressing the aero-optical effect lies in mitigating density pulsations. Currently, there are two primary approaches: one involves reducing the intensity of density pulsations through cooling; and the other aims to disintegrate large-scale vortex structures into smaller ones via flow control, thereby diminishing the intensity of density pulsations [21]. In this study, we will investigate the corrective impact of the latter method on aero-optical effects. The model employed remains based on the THAAD-like seeker model described above, with a series of MVGs positioned 4 mm upstream from the window, as depicted in Figure 3. The dimensions of the MVGs are indicated in Figure 3, with a spacing of 1 mm between adjacent MVGs, as denoted in the lower right corner.

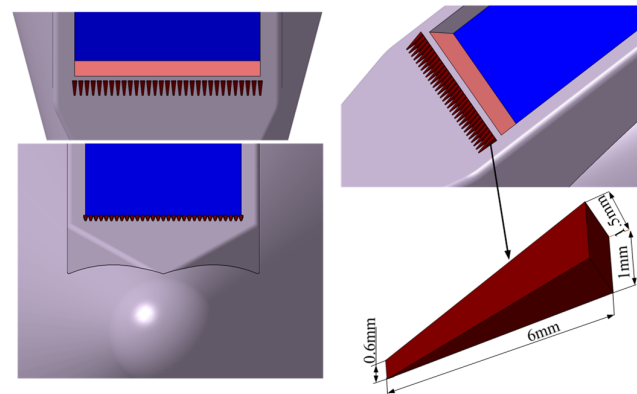


Figure 3. Schematic diagram of MVGs.

In order to further optimize the flow field structure near the optical window, a suction device is introduced at the compression corner adjacent to the window end, aiming to investigate whether it can enhance the light transmission performance of the flow field. The blow–suction control diagram is depicted in Figure 4 below.

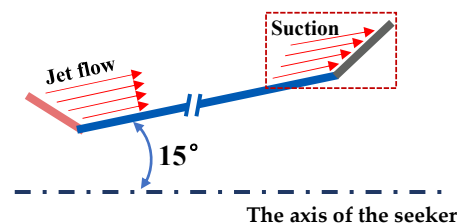


Figure 4. Schematic diagram of blow and suction control near optical window.

Table 2. Incoming flow parameters and cooling jet parameters.

Case	Flow Field Parameter				Blowing		MVGs	Suction
	Ma_∞	P (Pa)	T (K)	P_j (kPa)	m_0 (kg/s)	θ		
0	3	5529	216	-	0	-	without	without
1	5	5529	216	-	0	-	without	without
2	3	5529	216	11	0.05	15°	without	without

Table 2. Cont.

Case	Flow Field Parameter				Blowing		MVGs	Suction
	Ma_∞	P (Pa)	T (K)	P_j (kPa)	m_0 (kg/s)	θ		
3	5	5529	216	19	0.05	15°	without	without
4	5	5529	216	19	0.05	90°	without	without
5	5	1197	227	4.1	0.05	15°	without	without
6	5	1197	227	4.1	0.05	90°	without	without
7	5	5529	216	19	0.01	15°	without	without
8	5	1197	227	4.1	0.01	15°	without	without
9	5	1197	227	4.1	0.01	90°	without	without
10	3	5529	216	-	0	-	with	without
11	3	5529	216	11	0.05	15°	with	without
12	5	5529	216	-	0	-	with	without
13	5	5529	216	19	0.05	15°	with	without
14	3	5529	216	11	0.05	15°	without	with
15	3	5529	216	11	0.05	15°	with	with
16	5	5529	216	19	0.05	15°	without	with
17	5	5529	216	19	0.05	15°	with	with

According to relevant references [23], the upper limit of the total mass of the cooling gas is 1 kg, and the time interval from the beginning of pneumatic heating to hitting the target is 10~20 s when the hood is removed. Therefore, in this section, where jet cooling research is conducted, a maximum jet flow rate of 0.05 kg/s should be used. Among other parameters, Ma_∞ represents the incoming Mach number, P_0 represents the incoming pressure at this height, T_0 represents the local static temperature, P_j represents the cooling jet pressure, and m_j represents the jet flow of a specific jet mode. An insufficient coverage of concave windows by low-pressure jets may occur when using small nozzle flow rates. Additionally, if there is too much difference between the internal and external pressures, causing low jet pressure values, reflux can occur at the nozzles, preventing proper expulsion of gas. Conversely, high-pressure jets may not effectively cover entire surfaces of optical windows, with low-temperature gases resulting in localized high-temperature areas near the cavities above the windows. This occurs because, after passing through concave windows, high-speed and intense friction generate these hotspots, as they meet incoming gases. The higher gas pressure within the jets leads to larger temperature values for those hotspots closer to optical windows. Furthermore, increasing jet pressure results in greater heat flux values on optical windows, along with increased air-medium-wall friction, leading to elevated temperatures on optical window surfaces. Therefore, it is important to set appropriate parameters for jets, based on the pressure matching method found in the literature [21], in order to achieve optimal cooling effects. The flow field parameters presented in Table 2 demonstrate a comparative analysis of various methods for correcting the aero-optical effects in this paper.

3. Methods and Validation

The optical transmission performance in this study is primarily assessed based on the flowchart depicted in Figure 5 below. Initially, a model similar to THAAD is created and meshed, followed by precise calculation of its flow field. Once the fine outflow field is obtained, the aero-optical program accesses the grid refractive index matrix of the study area. By inputting the coordinates of the initial incident point and incident angle, the principal program of geometric optics determines whether or not the incident point falls within the calculation area and calculates its corresponding grid number based on its coordinates. Interpolation is then used to calculate refractive indices (n_0 and n_1) at both incident light and refracted light locations within their respective grids, while applying refraction law to determine if light reaches the surface of an optical window by crossing into another grid boundary. If it fails to reach said surface, the tracing process for the

aforementioned light will be repeated accordingly. Finally, once it does reach said surface, the tracing process concludes with outputting the evaluation parameters pertaining to aero-optical effects. A detailed introduction to the evaluation parameters for aero-optical effect assessment, as well as the flow field calculation method are provided below.

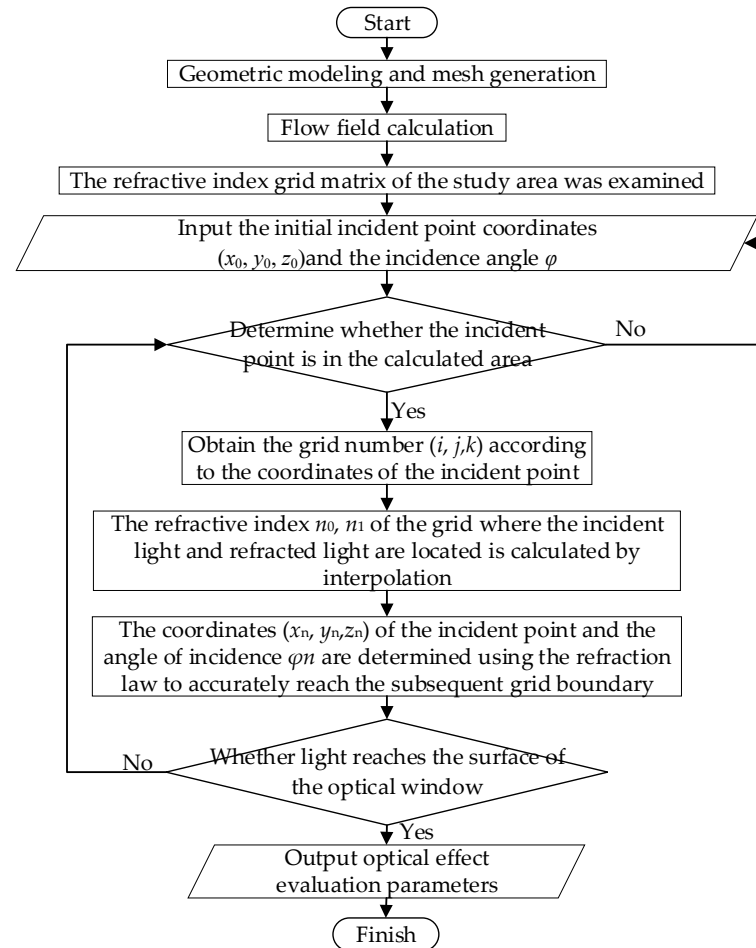


Figure 5. Flowchart of ray tracing for aero-optical effects.

3.1. Evaluation Parameters for Aero-Optical Effect

From a physical point of view, the aero-optical effect is essentially a phenomenon of fluid–optical interaction occurring in an optically turbulent flow such as a mixing layer over an optical window, as shown in Figure 6 (Schematic diagram of aero-optical effects).

The distance a beam travels in a flow field is usually expressed as an optical path length (OPL). The OPL is calculated based on geometric optics theory and can be obtained using the ray-tracing method, which mainly assumes that the rays composing the beam propagate through a grid, and that a single grid is taken to be a cell with an isotropic homogeneous fluid inside. For an arbitrary ray, its OPL can be expressed as the following equation [24]:

$$OPL = \sum n_i \cdot L_i \quad (1)$$

where n_i is refractive index of the fluid in the i -th grid on the ray transmission path, L_i is the ray transmission distance in the i -th grid, and the refractive index of the fluid can be converted from its density by the Gladstone–Dale relation, as follows:

$$n = 1 + \rho K_{GD} \quad (2)$$

$$K_{GD} = 2.2244 \times 10^{-4} \left(1 + \frac{4.51 \times 10^{-3}}{\lambda^2} \right) \quad (3)$$

where n is the refractive index, ρ the density, and K_{GD} represents the Gladstone–Dale coefficient that weakly depends on λ . With density expressed in m^3/kg and wavelength expressed in μm , K_{GD} can be approximately acquired by λ . In this study, the infrared image sensor is sensitive to wavelengths ranging from 3 to 5 μm , so a 4 μm wavelength was computed.

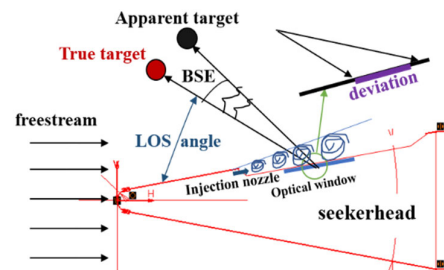


Figure 6. Schematic diagram of aero-optical effects.

In practical applications, the optical path difference (OPD) is a quantity of more interest than the OPL , and moreover, it is easier to obtain, which is defined as follows:

$$OPD(x, y, z, t) = OPL(x, y, z, t) - \langle OPL(x, y, z, t) \rangle \quad (4)$$

where the angle brackets denote the spatial average of the OPL s of all rays, where OPD_{rms} is the root mean square of the OPD s for all rays. From the equations listed above, it can be seen that the image deviation, BSE, and OPD are three key metrics to evaluate the aero-optical transmission effects, so all of them are used as evaluation parameters in this work. Figure 7 shows the distorted wavefronts, the $\langle OPL \rangle$, and the OPD formed by the rays passing through the inhomogeneous flow field.

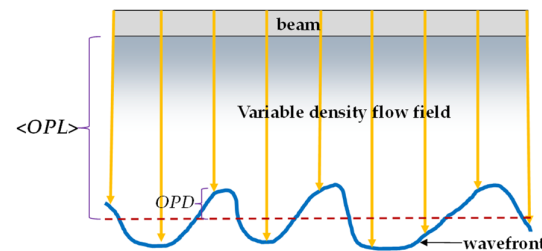


Figure 7. The ray passing through the nonuniform flow field forms the distorted wavefront, $\langle OPL \rangle$, and OPD .

The difference in OPL directly indicates the extent of wavefront distortion. A larger OPD corresponds to a greater degree of wavefront distortion. By multiplying the wave number with the optical path difference, we can obtain a measure of the phase difference, which consequently reflects the degree of wavefront phase distortion, or simply put, the wavefront distortion itself. The formula below can be employed for quantifying wavefront distortion.

$$w(x, y, z, t) = \frac{2\pi}{\lambda} OPD(x, y, z, t) \quad (5)$$

3.2. Numerical Simulation Method

The RANS method exhibits high computational efficiency but low-fidelity for turbulence simulation in practical engineering, while the LES method offers high-fidelity at the expense of computational efficiency. In order to accurately capture the flow structure of the outflow field of the optical window in the proposed seeker model with appropriate computational efficiency, the high-fidelity Detached Eddy Simulation (DES) method is used to numerically simulate the flow field around the optical window.

The SST-DES method is a DES hybrid method proposed by Strelets [25] in 2001 on the basis of the two-equation SST model, and its governing equations are as follows:

$$\frac{\partial k}{\partial t} + U_j \frac{\partial k}{\partial x_j} = \frac{1}{\rho} P_k - \beta^* k \omega F_{\text{DES}} + \frac{1}{\rho} \cdot \frac{\partial}{\partial x_j} \left[\left(\mu + \frac{\mu_t}{\sigma_k} \right) \frac{\partial k}{\partial x_j} \right] \quad (6)$$

$$\frac{\partial \omega}{\partial t} + U_j \frac{\partial \omega}{\partial x_j} = \frac{1}{\rho} P_\omega - \beta \omega^2 + \frac{1}{\rho} \cdot \frac{\partial}{\partial x_j} \left[\left(\mu + \frac{\mu_t}{\sigma} \right) \frac{\partial \omega}{\partial x_j} \right] + 2(1 - F_1) \frac{1}{\sigma_{\omega_2}} \cdot \frac{1}{\omega} \cdot \frac{\partial k}{\partial x_j} \cdot \frac{\partial \omega}{\partial x_j} \quad (7)$$

$$S_{D,k} = -\beta^* k \omega F_{\text{DES}} \quad (8)$$

where k and ω are the turbulent kinetic energy and specific dissipation rate; μ_t is the dynamic turbulent eddy viscosity coefficient. P_k and P_ω are the generated terms of Equations (4) and (5), respectively. β^* , σ_k , σ_ω , σ_{ω_2} are the SST model's constants. ρ is the density; F_{DES} is the switching function of the SST-DES method, and its expression is as follows:

$$F_{\text{DES}} = \max \left[\frac{L_{\text{RANS}}}{C_{\text{DES}} \Delta}, 1 \right] \quad (9)$$

The length scale of the RANS model, LRANS, is defined as $L_{\text{RANS}} = k / (\beta^* \omega)$. The value of F_{DES} is 1 in proximity to the wall and exceeds 1 further away from it. For additional details regarding other parameters and expressions in Equations (4)–(6), please refer to [26].

The SST-DDES method was proposed by Menter et al. [27]. The switching function of the SST-DES method was modified to reduce the dependence of the method on the grid. The switch function expression after its change is as follows:

$$F_{\text{DDES}} = \max \left[(1 - F_{\text{SST}}) \frac{L_{\text{RANS}}}{C_{\text{DES}} \Delta}, 1 \right] \quad (10)$$

In the formula, F_{SST} is equal to F_1 or F_2 , and F_1 and F_2 are the boundary layer identification functions inherent to the SST model, whose value range is similar to r_d , and are the delay factors of the SST-DDES method.

$$r_d = \frac{v_t + v}{\sqrt{U_{i,j} U_{i,j}} \kappa^2 d_w^2} \quad (11)$$

where v_t is the moving eddy viscosity coefficient; v is the viscosity coefficient of moving molecules; κ is the Karman constant, with a value of 0.41. r_d is approximately equal to 1 in the logarithmic layer, and then gradually decreases to 0 in the viscous top layer. This simulation employs the Roe scheme for spatial dispersion and the LU-SGS scheme for time advancement. The initial flow field is obtained through steady calculations, followed by unsteady calculations consisting of 20,000 steps, with a physical time step of 10^{-7} s.

3.3. Validation of Numerical Method

Although the geometric model of the step compression corner after supersonic flow is straightforward, the flow structure of separation and reattachment exhibits significant similarities with the flow field structure outside the window of future hypersonic seekers. As a numerical example, Settles's [28] cavity compression corner model is selected as an experimental reference to verify the accuracy of the turbulence model and align with the flow problem studied in this paper. The step height $h = 2.54$ cm, the rear wall length $l = 6.19$ cm, and the compression corner angle $\theta = 20^\circ$. The mesh generated by the pointwise and geometric parameters is illustrated in Figure 8 below.

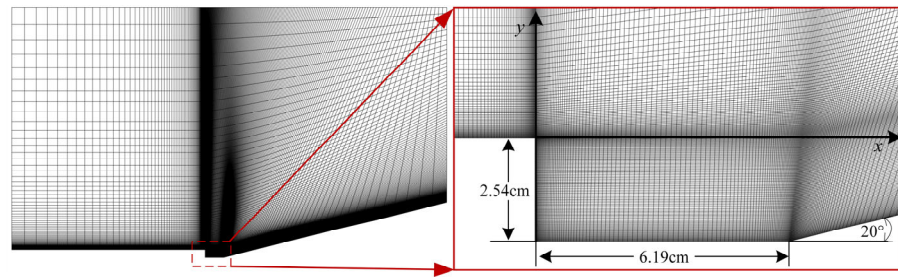


Figure 8. Mesh generation for supersonic cavity-compression corner and geometric parameters.

FLUENT (2023 R1) is a popular computational fluid dynamics (CFD) software, which has advanced numerical calculation methods. In this paper, FLUENT was used to calculate the temperature, velocity, and pressure distribution of the flow field. Under the working conditions of the wind tunnel test, the incoming Mach number is 2.92, the incoming flow temperature is 95.37 K, and the incoming pressure is 21.24 kPa. Initially, a steady flow field structure is obtained through steady calculation; subsequently, an unsteady flow field structure is acquired using the DDES method, with $C_{DES} = 0.78$ and $\beta^* = 0.09$, while employing the Roe format for the spatial discretization technique. LU-SGS performs time propulsion with physical time steps set at 3×10^{-7} s. The contour diagram of the time-averaged Mach number and eddy viscosity of the flow field is depicted in Figure 9, while Figure 10 illustrates the root-mean-square distribution of the flow velocity at stations $x/h = 1.5$ and $x/h = 3.5$.

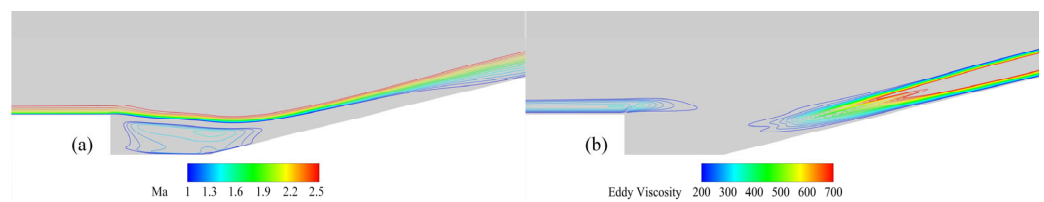


Figure 9. (a) Iso-contours of time averaged Ma and (b) time averaged eddy viscosity.

It can be observed that at the $x/h = 1.5$ station, the position is situated within the cavity of the specimen, resulting in a complex flow structure. The velocity results obtained from the numerical simulation exhibit excellent agreement with the experimental values. At the $x/h = 3.5$ location, it is positioned on the wall of the compression corner, behind the cavity. In comparison to the flow inside the cavity, the flow structure here appears relatively simpler; however, the calculated values slightly exceed the test values at $y/h = 0 \sim 0.5$ range. To summarize, employing the SST-DDES method yields satisfactory RMS speed outcomes, and thus this method can be used to study the problem in this paper.

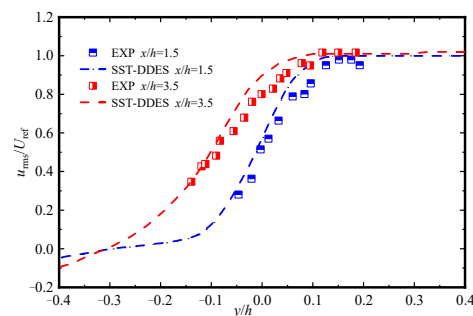


Figure 10. Root-mean-square distribution of flow velocity at different stations.

3.4. Grid Independence Study

For complex flow phenomena, the selection of grid scale generally relies on the experience of basic flow models; however, more reliable results can be obtained through

grid convergence analysis. The DDES method imposes high requirements on the mesh size, thus this section enhances the encryption of the flow field area near the window. The mesh density along the wall of the back step is increased, with a mesh scale close to the wall set at 50 μm . After encryption, there are approximately 700,000, 1 million, and 1.3 million grids near the window, respectively.

Firstly, three types of meshes with varying levels of encryption were utilized to compute the characteristics of the window flow field without jet flow, specifically referred to as the Case1 ($Ma = 5$, without jet) example. The grid independence was evaluated by analyzing the longitudinal density fluctuation in the flow field as an indicator. As illustrated in Figure 11, no significant disparity is observed in the pulsation trend among the three sets of numerically simulated grid results. From a magnification perspective, it can be noted that the sparse mesh fails to adequately capture detailed variations occurring at locations where the density changes more abruptly. Conversely, medium and refined meshes demonstrate superior capability in capturing and yielding similar outcomes for fluid dynamics.

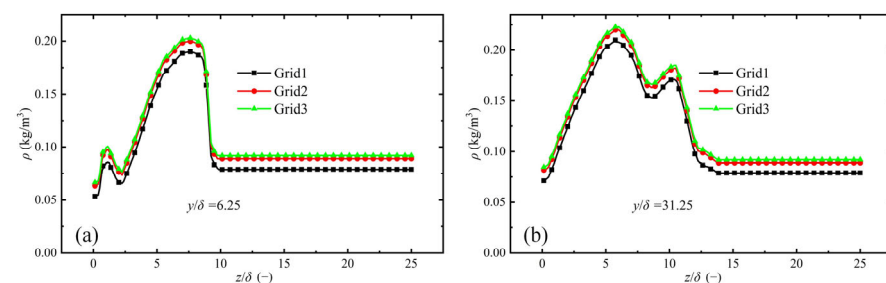


Figure 11. The longitudinal density pulsations of three different grids are compared at (a) $y/\delta = 6.25$ and (b) $y/\delta = 31.25$.

The spanwise distribution of flow velocity in the shear layer formed by a cooling jet and distant incoming flow under the Case3 ($Ma = 5$, jet flow = 0.05 kg/s, $\theta = 15^\circ$) working condition is depicted in Figure 12. It can be observed that there is a significant variation in velocity along the spreading direction, with slightly lower speeds at the center compared to both ends of the window. Based on the calculation results obtained from three different density grids, it is found that the results obtained using medium-density and encrypted grids exhibit similarity, with a maximum error of 3 m/s, accounting for less than 1%. Therefore, for flow simulation near the optical window, employing medium and encrypted meshes is deemed feasible. In order to ensure accuracy while reducing computational efforts, this paper adopts a medium grid for subsequent calculations.

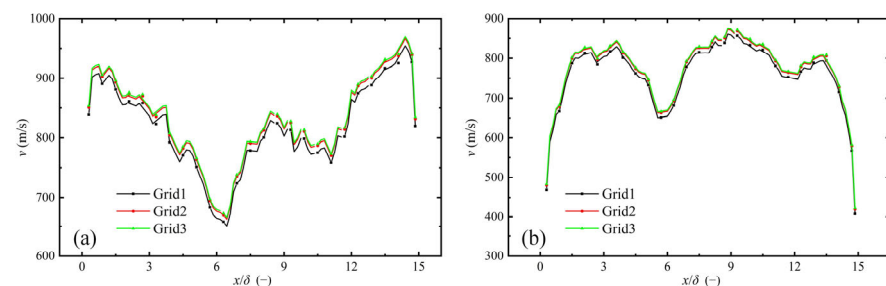


Figure 12. The flow velocity profiles of three meshes were compared at (a) $y/\delta = 18.75$ and (b) $y/\delta = 43.75$.

4. Results and Discussion

The main focus of this section is to investigate the temperature distribution of the optical window and analyze the impact of flow field on optical transport phenomena in the presence of jet flow, MVGs, and suction. Additionally, it discusses measures for correcting aero-optical effects through integrated flow control.

4.1. Aero-Optical Effect Correction with Jet Flow

This section focuses on investigating the impact of different jet modes on the aerodynamic heating effect of the seeker's optical window and analyzing the disparities in aero-optical effects caused by two nozzle modes with varying orientations.

The thermal radiation of the infrared window is a crucial factor that limits infrared imaging. Therefore, the primary issue addressed by the cooling jet is the degradation of imaging quality caused by aerodynamic heat, resulting in a decrease in window temperature. Figure 13 illustrates the distribution of window temperature under four different operating conditions. Figure 13a represents the temperature distribution without jet cooling. As velocity increases, the flow field experiences stronger compression, leading to higher peak pressure and density on the optical window. Simultaneously, the shock layer at the front end of the hood becomes thinner, with a smaller shock angle. Due to this compression effect, as well as an increased flow-field temperature and heat flux on the optical window, the friction between the air medium and the wall intensifies, while imposing higher material requirements for optical windows with elevated wall temperatures. Consequently, higher speeds necessitate greater reliance on jet cooling to mitigate aerodynamic effects and safeguard optical windows from damage. It can be observed that at 5 Ma, aerodynamic heating causes an increase in window temperature up to 900 K. When the airflow encounters a step in its direction of flow, its velocity decreases, causing the conversion of kinetic energy into internal energy and a subsequent rise in temperature occurs. Upon leaving the optical window surface, encountering a compression corner further reduces the airflow speed while generating additional heat energy. Henceforth, this results in a high-low-high trend within the surface temperature distribution; however, these variations are minimal, with an approximate difference of only 30 K.

The temperature distribution of the window during jet cooling parallel to the window at a speed of 5 Ma is illustrated in Figure 13b. It is evident that an effective reduction in window temperature can be achieved when the jet flow rate is 0.05 kg/s, resulting in an average temperature decrease to 150 K. However, as the cooling gas gradually dissipates along the direction of flow due to its parallel nature with respect to the window, the cooling effect diminishes and consequently leads to a gradual increase in window temperature.

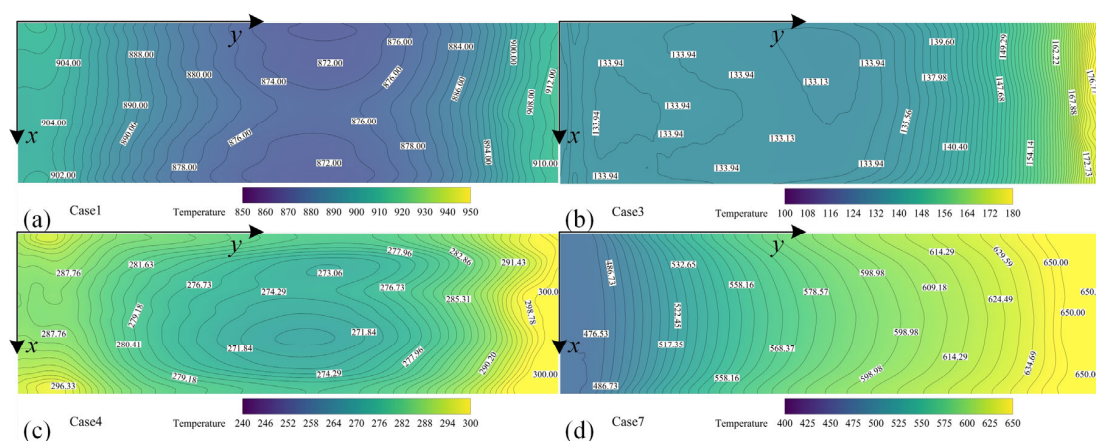


Figure 13. Comparison of optical window temperature distribution under 5 Ma, $P = 5529$ Pa conditions: (a) without jet; (b) jet flow = 0.05 kg/s, $\theta = 15^\circ$; (c) jet flow = 0.05 kg/s, $\theta = 90^\circ$; (d) jet flow = 0.01 kg/s, $\theta = 15^\circ$.

In Figure 13c, we observe the window temperature distribution when employing a perpendicular jet relative to the seeker axis under identical flight Mach number conditions. It becomes apparent that direct exposure of the cooling gas on the window surface is absent compared to the Case3 ($\theta = 15^\circ$) mode, resulting in a weaker reduction in window temperature. This discrepancy primarily arises from significant changes in incoming flow velocity, caused by expansion waves formed upon encountering vertical jets, leading to

substantial reflux near the nozzle and dissipation of gas kinetic energy. Consequently, this results in inferior temperature drop effects compared to those observed with parallel jets. Figure 13d primarily investigates whether reducing jet flow has a notable impact on window cooling performance. As depicted in this figure, even when utilizing a reduced jet flow rate of 0.01 kg/s, there remains some degree of reduction in window temperature; however, due to limited amounts of cooling gas available at such low rates, rapid consumption occurs, along with diminishing effectiveness along the direction of flow.

Subsequently, the impact of different jet angles and flow rates on the reduction of window temperature is compared under decreased inlet pressure conditions, as depicted in Figure 14 below. Figure 14a illustrates the temperature distribution across the window when a jet flow rate of 0.05 kg/s is employed at Mach 5 flight speed and an altitude of 30 km. The figure clearly demonstrates a significant cooling effect on the window, with minimal temperature fluctuations, which can be attributed to reduced total pressure resulting in diminished external interference from cooling gas and facilitating efficient heat absorption near the window surface. In Figure 14b, where a perpendicular jet mode is utilized, it becomes evident that temperatures near the rear step are higher, due to vertical jets disrupting the normal free flow and causing increased separation zones, along with larger return regions at the back step, consequently leading to elevated temperatures. Excessive cooling gas carried downstream by free flow enhances cooling effectiveness for downstream windows compared to upstream positions. The temperature distribution of the window gradually increases in Figure 14c,d as the jet flows decrease, while still maintaining directionality towards flow progression. In summary, both jet modes improve the wall temperatures of windows compared to back-step flow fields without jets. Among them, tangentially parallel window jet cooling exhibits superior performance over vertical axis jet cooling.

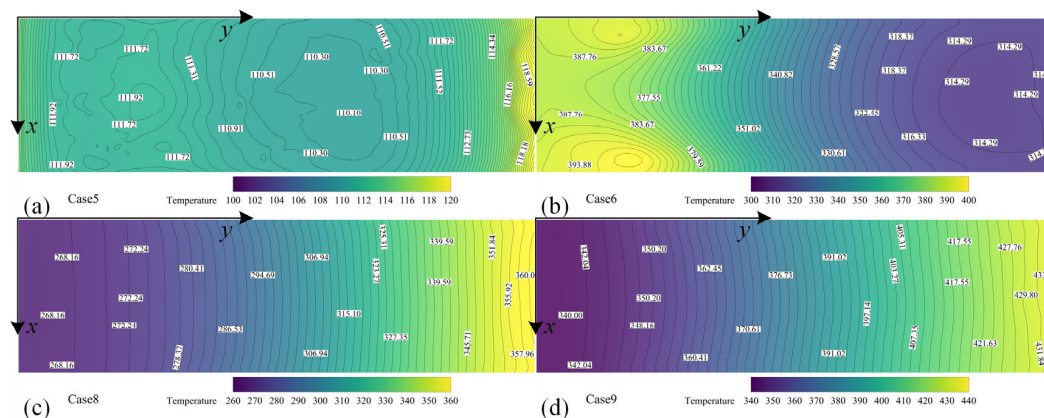


Figure 14. Comparison of optical window temperature distribution under $Ma = 5$, $P = 1197$ Pa conditions: (a) jet flow = 0.05 kg/s, $\theta = 15^\circ$; (b) jet flow = 0.05 kg/s, $\theta = 90^\circ$; (c) jet flow = 0.01 kg/s, $\theta = 15^\circ$; (d) jet flow = 0.01 kg/s, $\theta = 90^\circ$.

The objective of this section is to investigate the correction method for mitigating the aero-optical effect, which arises from the combined influence of temperature variations in the optical window wall and flow field development. Essentially, the aero-optical-induced optical transport effect is caused by density fluctuations in the flow field. In theory, suppressing density fluctuations in the flow field can effectively suppress the aero-optical effect. Figure 15 presents density pulsation information along the symmetric surface of the optical hood for different flow conditions. Based on our analysis, jet cooling proves effective in reducing aerodynamic heat radiation through windows. However, as shown in Figure 15a, under the Case0 and the Case1 (without jet) conditions, there is minimal density pulsation, along with negligible additional disturbance effects. Conversely, after introducing jet flow (Case2 and Case3), due to disturbances induced by jet flow, density pulsations increase noticeably upstream within the flow mixing zone but rapidly decrease downstream, along with fluid motion directionality. Comparing Case4 ($P = 5529$)

and Case6 ($P = 1197$) reveals that increasing flight altitude significantly reduces density fluctuation at identical Mach numbers. Figure 15b compares how decreasing jet flow affects density pulsation levels; it becomes evident that only reducing jet flow leads to significant reductions in density pulsation under similar jet patterns—thus indicating an expected weakening of aero-optical transmission effects as well. Further comparison between Case8 ($\theta = 15^\circ$) and Case9 ($\theta = 90^\circ$) demonstrates that changes in jet angle also result in reduced impact on fluid field densities owing to the cooling gas reduction.

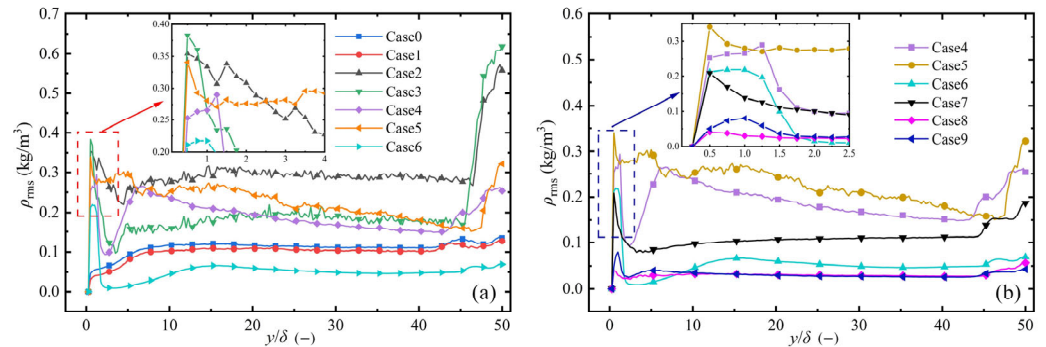


Figure 15. ρ_{rms} distribution in flow direction under different flow field conditions: (a) Case0 to Case6; (b) Case4 to Case9.

The density pulsation information in the flow field is directly associated with the aero-optical effect problem. Figure 16 illustrates the RMS density distribution in the lateral direction of the optical window at $y/\delta = 6.25$. Based on the aforementioned analysis, jet cooling proves effective in mitigating the aerodynamic heat radiation phenomena on the window surface. However, as depicted in Figure 16a, under the Case0 and Case1 (without jet) conditions, there is relatively uniform ρ_{rms} distribution in the lateral direction, with minimal influence from additional disturbances. Nevertheless, upon introducing jet flow (Case2 condition), significant density fluctuations are observed upstream of the flow mixing zone due to the disturbance caused by jet flow interaction. Figure 16b compares how reduced jet flow impacts density pulsation.

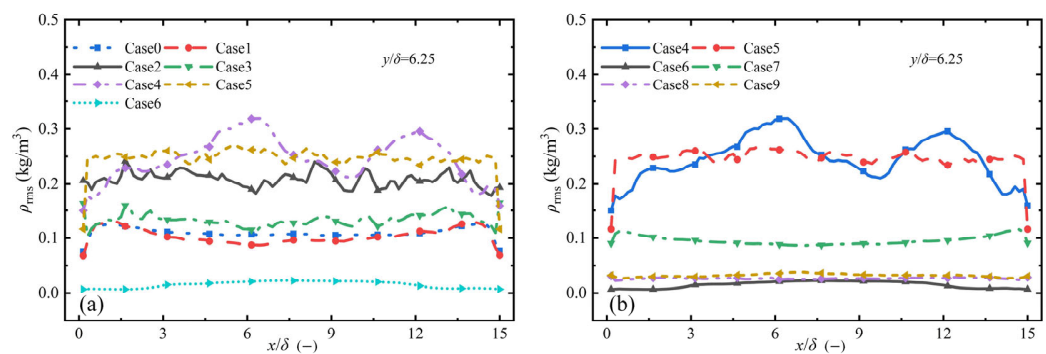


Figure 16. ρ_{rms} distribution in spanwise direction under different flow field conditions: (a) Case0 to Case6; (b) Case4 to Case9.

It can be observed that when comparing Case5 (jet flow = 0.05 kg/s) with Case8 (jet flow = 0.01 kg/s) under similar jet modes, reduced jet flow significantly diminishes density pulsation while also weakening aero-optical transmission effects accordingly. Furthermore, compared to Case8 ($\theta = 15^\circ$) and Case9 ($\theta = 15^\circ$), decreased gas injection leads to a reduction in ρ_{rms} , influenced by different jet modes.

It can be seen from Figure 17 that the RMS of optical window density varies greatly along the longitudinal direction, mainly in the vicinity of the near-wall turbulent boundary layer and shock wave in the flow field. The parameters of the flow field in the turbulent boundary layer near the wall of the (high) supersonic flow field have a large gradient due to the influence of

turbulence, so there will be a large deflection phenomenon when the ray passes through the flow field of the turbulent boundary layer. Since the density field of the (hypersonic) supersonic flow field has a great gradient when passing through the shock wave, the refractive index field determined by the density field will naturally have a large gradient near the shock wave, so the ray will also have a large deflection when passing through the shock wave.

The comparison of Case1 (without jet) and Case3 (jet flow = 0.05 kg/s) in Figure 17a shows that, due to the difference between the two conditions, only with or without jet flow, it can be seen that when the jet flow is tangent parallel to the window, a great gradient value appears near the window wall, and the root mean square of density rises sharply, reaching a maximum value of $\rho_{rms(max)} = 0.7 \text{ kg/m}^3$. It can be concluded that the light will have a large deflection here. However, after $z/\delta = 5$, the ρ_{rms} values under the two conditions tend to be consistent due to the weakening of the near-wall flow field interference. Case4 (jet flow = 0.05 kg/s, $\theta = 90^\circ$) is the case of jet flow perpendicular to the axis, and the root mean square of density near the wall also has an extreme value of $\rho_{rms} \approx 0.27$, but because it is upstream of the window, it is close to the shock wave. In Figure 17a, the dimensionless distance along the longitudinal direction is denoted as z/δ . Here, $z/\delta = 0$ corresponds to the window surface, while $z/\delta \approx 7$ is in close proximity to the shock layer of the seeker's outflow field. It can be observed that ρ_{rms} exhibits an extreme state under all working conditions at this location. Due to the perpendicular orientation of Case4 jet with respect to the seeker axis, the emitted cooling gas encounters and interacts with the shock layer of the seeker's flow field, resulting in the formation of an interference structure. Consequently, the ρ_{rms} near the shock wave tends to be slightly larger compared to its state without jet.

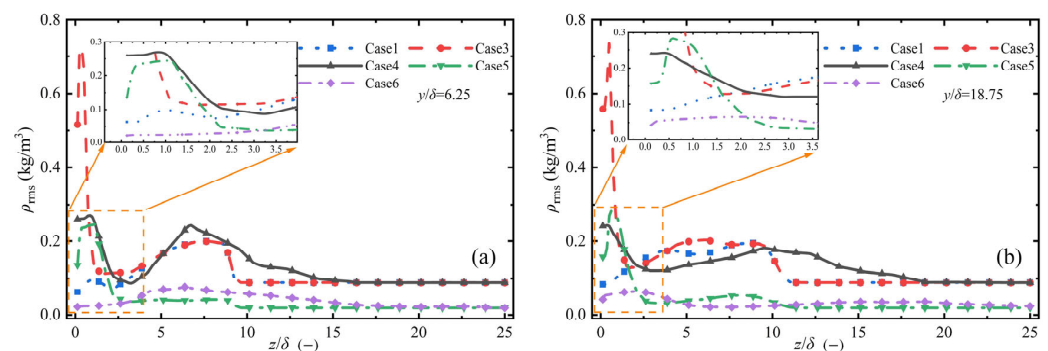


Figure 17. ρ_{rms} distribution in longitudinal direction under different flow field conditions: (a) $y/\delta = 6.25$; (b) $y/\delta = 18.75$.

4.2. Aero-Optical Effect Correction with Micro Vortex Generators (MVGs)

The temperature distribution of the optical window under $Ma = 3$ without jet cooling is compared in Figure 18 below. It can be observed that the temperature at the upstream position of the optical window is slightly higher with MVGs than without MVGs.

Overall, there is little difference in the surface temperature between the two windows, which also indicates that MVGs will not play a role in cooling the windows without jet cooling measures.

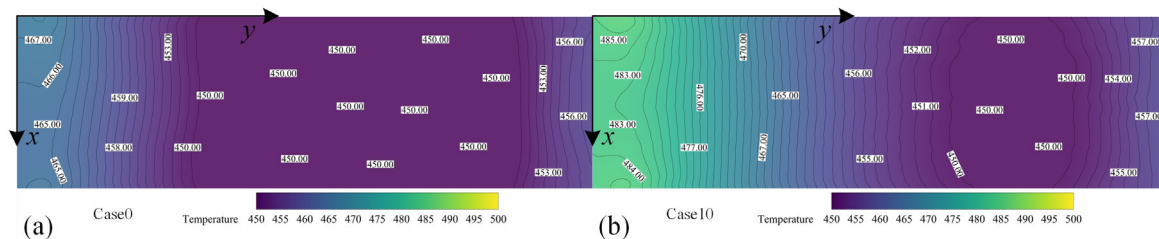


Figure 18. Comparison of optical window temperature distribution under $Ma = 3$ without jet cooling: (a) without MVGs, (b) with MVGs.

In order to directly compare the flow field with and without MVGs, Figure 19 illustrates the refractive index distribution near the optical window at $Ma = 3$ without jet cooling. Similarly, Figure 20 depicts the refractive index distribution near the optical window at $Ma = 5$ without jet flow. During high-speed flight, the flow field near the seeker undergoes compression into layers, resulting in air flow adherence to the seeker wall and the formation of a shock layer. The flow in the shock layer exhibits a narrow front and wide rear distribution. Considering the overall shape depicted in Figure 1a and the installation position shown in Figure 3 for MVGs, it can be concluded that their presence does not affect the bow shock wave present on the outer layer of the seeker. Additionally, due to turbulent boundary layer effects and associated low density, there is also a decrease in the refractive index near walls. However, regardless of whether $Ma = 3$ or $Ma = 5$, both cases exhibit similar distribution patterns for refractive indices without significant gradients.

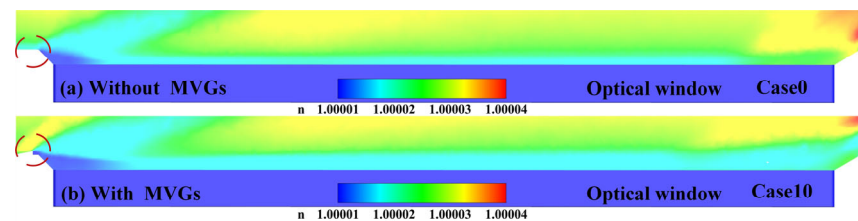


Figure 19. Comparison of refractive index distribution near the optical window at $Ma = 3$ without jet cooling.

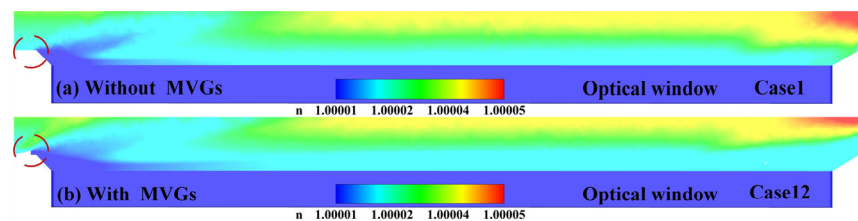


Figure 20. Comparison of refractive index distribution near the optical window at $Ma = 5$ without jet cooling.

The temperature distribution in the optical window with jet cooling ($Ma = 3$, flow jet = 0.05 kg/s) is compared in Figure 21, where (a) is without MVGs and (b) is with MVGs.

Due to the concave structure of the optical window, resistance is encountered when high-speed air flows through the vortex generator, resulting in a reduction in air velocity and transfer of heat from “extra” kinetic energy into the turbulent boundary layer, leading to a diminished cooling effect. The average surface temperature of the window with MVGs is approximately 30 K higher compared to that without MVGs, yet it generally satisfies the requirement for window cooling. However, the addition of MVGs results in a gradual increase in the window temperature distribution along the flow direction (Y-axis).

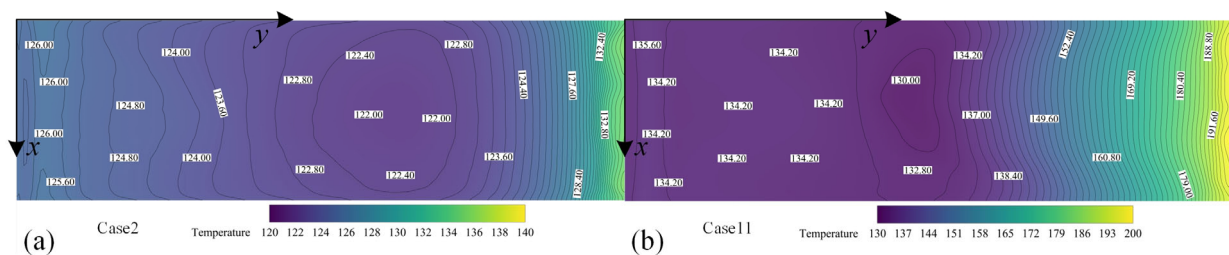


Figure 21. Comparison of optical window temperature distribution under $Ma = 3$ with jet cooling: (a) without MVGs, (b) with MVGs.

The introduction of the cooling jet leads to a highly non-uniform refractive index distribution within the investigate area, primarily characterized by slight variations along the flow direction parallel to the wall (y -axis). Along the direction perpendicular to the window surface (z -axis), there is an initial increase, followed by a decrease in refractive index distribution, as illustrated in Figure 22. The addition of jet gas significantly enhances the complexity of flow field structure, resulting in distinct shear layer, mixing layer, and turbulent boundary layer structures near the window. Furthermore, both density and density gradient experience substantial increments within the flow field. A comparison between Figure 22a,b reveals that after incorporating micro-vortex generators, the pulsations in proximity to the window are considerably reduced, as indicated by the red dashed circle in the figure below.

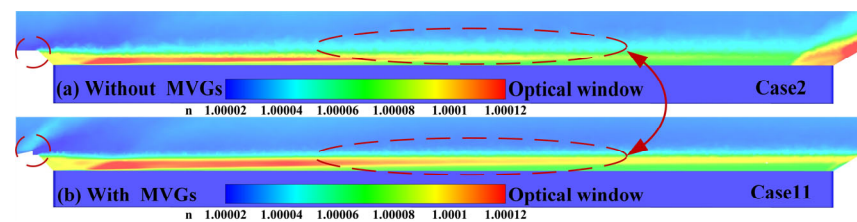


Figure 22. Comparison of refractive index distribution near the optical window at $Ma = 3$ with jet cooling.

The comparison of temperature distribution in the optical window at $Ma = 5$ for jet cooling is illustrated in Figure 23 below. It can be observed that the addition of MVGs leads to a more uniform temperature distribution across the entire window, reducing the temperature difference between the highest and lowest points to only 4.5 K. In contrast, without MVGs, temperatures gradually increase along the flow direction, resulting in a significant difference of 42.8 K. This non-uniform temperature distribution induces stress-induced deformation in the window material, exacerbating both the elastic and aero-optical effects. Thus, it can be concluded that micro-vortex generators effectively enhance temperature uniformity within cooled jets.

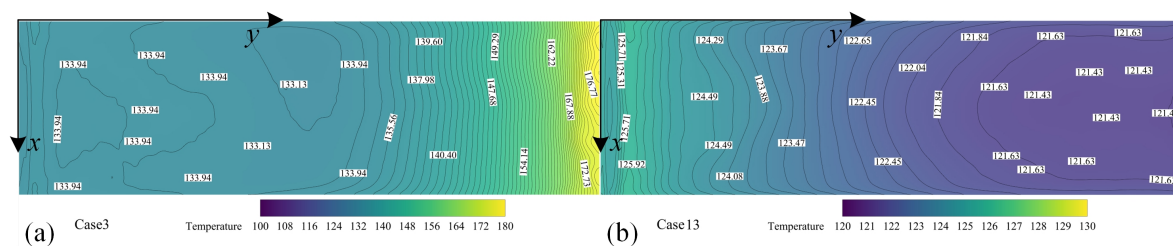


Figure 23. Comparison of optical window temperature distribution under $Ma = 5$ with jet cooling: (a) without MVGs, (b) with MVGs.

The turbulent boundary layer plays a crucial role in generating the aero-optics effect. In flow fields with higher Mach numbers, the stratification of the flow field becomes more pronounced due to increased compressibility. As shown in Figure 24, the addition of MVGs can still inhibit the formation of large-scale vortex structures.

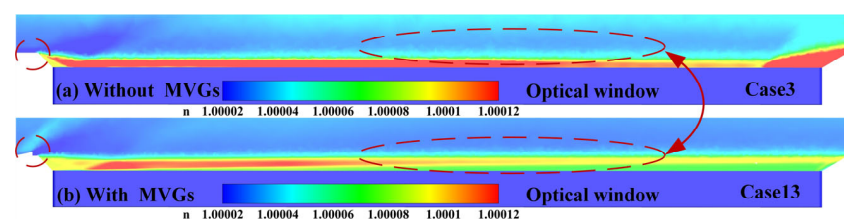


Figure 24. Comparison of refractive index distribution near the optical window at $Ma = 5$ with jet cooling.

The comparison of wavefront aberration with and without MVGs is depicted in Figure 25 below. It can be observed from Case10 ($Ma = 3$, without jet, with MVGs) that MVGs do not modify the amplitude of wavefront distortion. This can be attributed to the fact that the overall density gradient in the shock layer remains unchanged during downstream airflow development, transitioning solely from a large-scale vortex structure to a small-scale one. Moreover, it demonstrates that passive flow control fails to effectively mitigate aero-optical-induced wavefront aberration.

After cooling with jet flow, it is evident that wavefront aberration increases uniformly along the flow direction, as depicted in Case2 (without MVGs). However, when MVGs are added, a series of “gullies” appear on the distorted surface due to their arrangement upstream of the window, as an array. The small vortices generated after airflow passes through MVGs affect optical transmission in the downstream flow field, leading to this phenomenon observed in Case11 ($Ma = 3$, jet flow = 0.05 kg/s, with MVGs). It is noteworthy that comparing Case0 ($Ma = 3$, without jet) and Case2 ($Ma = 3$, jet flow = 0.05 kg/s) reveals an overall improvement in wavefront distortion values after incorporating the jet flow. This finding further supports the notion that a significant portion of imaging distortion arises from contributions by mixed layers and turbulent boundary layers.

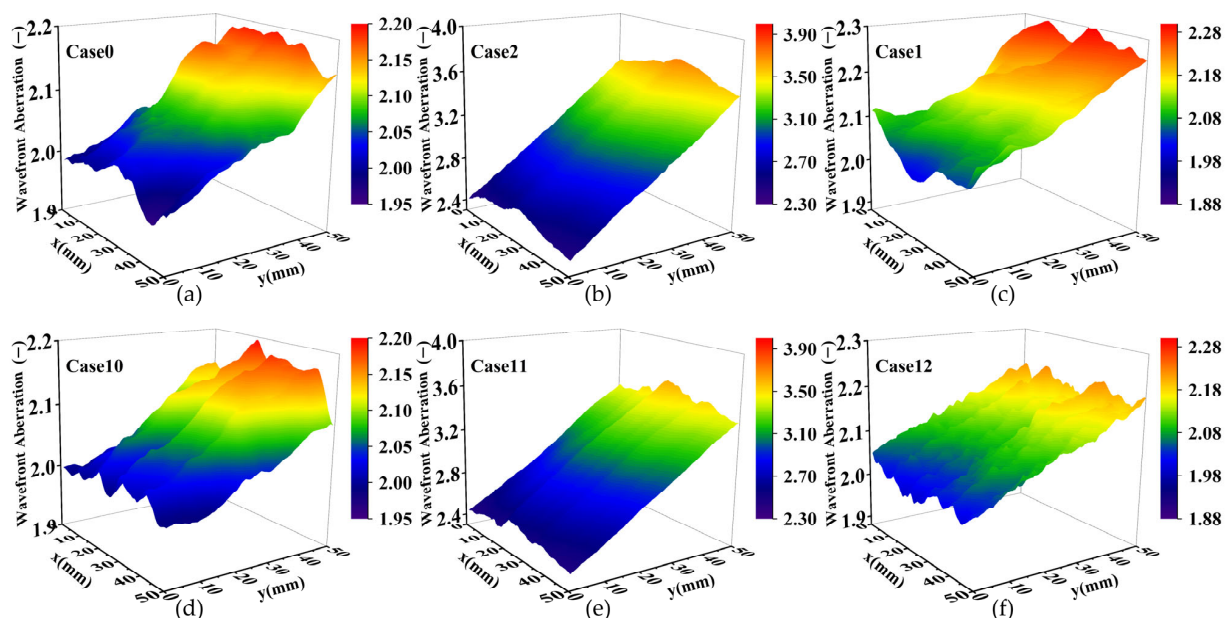


Figure 25. Comparison of optical wavefront aberration under different flow field conditions (Case0~Case2 conditions without MVGs, Case10~Case12 conditions with MVGs).

When investigating higher Mach numbers' impact on wavefront distortion with added MVGs, it was found that the aforementioned “gully” phenomenon occurs due to their inclusion—resulting in waveform distortions seen under Case12 ($Ma = 5$, without jet, with MVGs) conditions. As Ma increases, relative to the results obtained under Case1 ($Ma = 5$, without jet, without MVGs) conditions, there is approximately a 21.7% decrease observed at peak distortions.

4.3. With Suction-Controlled Aero-Optical Effect Correction

According to the prediction, the implementation of inspiratory control will further suppress the development of large-scale vortex structures, resulting in a more flattened disturbed flow field near the window. The stratification along the z -axis will become more pronounced, and density fluctuations will be further reduced. In high Mach number flight, one of the primary challenges for optical detection is mitigating aerodynamic thermal radiation effects on optical windows. Figure 26 illustrates the temperature distribution across the window, with suction control applied. It can be observed that with or without MVGs, adding suction control to the compression corner downstream effectively maintains

a low window temperature gradient. Compared to Figure 21b, there is an enhanced uniformity in the window temperature distribution achieved through this approach.

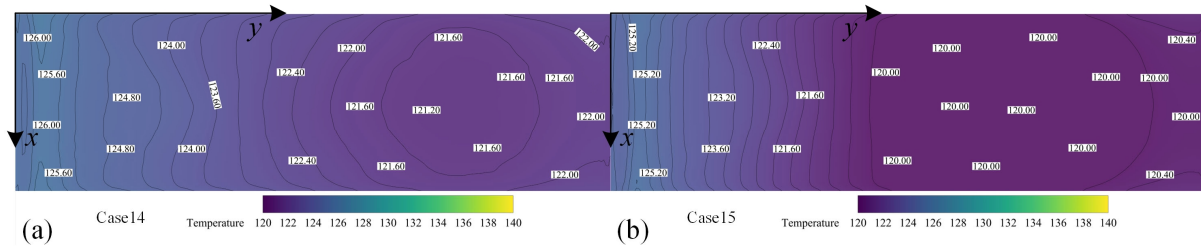


Figure 26. Comparison of optical window temperature distribution (a) without MVGs and (b) with MVGs under blow–suction cooling control at $Ma = 3$.

The spatial distribution of the density field in the vicinity of the hypersonic/supersonic optical window directly influences its optical detection performance. However, when employing jet cooling and inspiratory rectification in the optical window, it not only introduces complex flow field structures such as shock waves and expansion waves but also experiences influences from mixed layers and turbulent boundary layers, resulting in a more pronounced spatial non-uniformity in the density field. Therefore, studying the refractive index field derived from the density field becomes crucial. Figure 27 illustrates the distribution of refractive index near the optical window under blowing–suction refrigeration control. In this figure, due to inspiratory control at the downstream compression corner of the window, there is an absence of observed complex flow field structures. Comparing with Figures 22a and 24a, the fluctuations in refractive index within the flow field are reduced while exhibiting a more distinct longitudinal distribution.

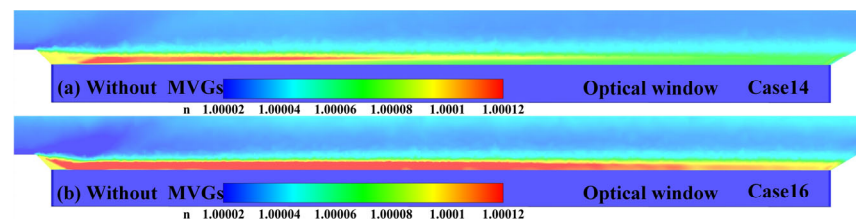


Figure 27. Comparison of refractive index distribution near the optical window: (a) $Ma = 3$, (b) $Ma = 5$.

The disturbance of the flow field at the compression corner downstream of the window is clearly evident in Figure 28 below. However, it can be observed that the inspiratory control in other regions of the window at $Ma = 3$ does not significantly enhance the refractive index fluctuation results of the flow field.

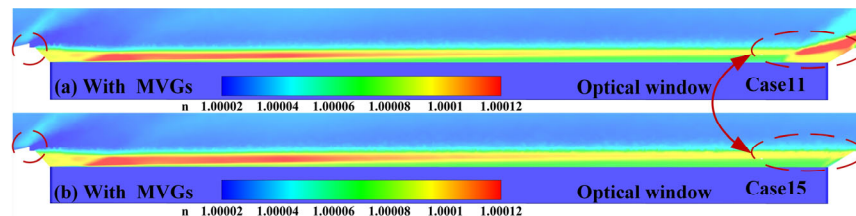


Figure 28. Comparison of refractive index distribution near the optical window under $Ma = 3$: (a) without suction, (b) with suction.

The flow results at higher Mach numbers reveal the significant impact of the inspiratory disturbance downstream of the window on the density distribution throughout the boundary layer. Figure 29b demonstrates a high-density region extending from upstream to the tail of the window near its wall, providing evidence for how this inspiratory disturbance contributes to stratification within the flow field and facilitates a reduction in phase inequality during ray transmission.

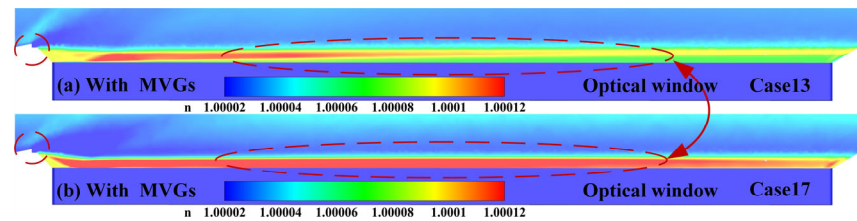


Figure 29. Comparison of refractive index distribution near the optical window under $Ma = 5$: (a) without suction, (b) with suction.

The following three comparison diagrams depict the wavefront distortion results under three conditions: only the jet without any inspiratory disturbance at 3 Ma, the addition of inspiratory disturbance, and the inclusion of MVGs and inspiratory disturbance, as illustrated in Figure 30. It is observed that incorporating an inspiratory disturbance can effectively reduce both the average and maximum values of wavefront distortion by 14.7%. A careful analysis reveals that although the mean value of wavefront distortion can be reduced, there remains an increasing trend in distortion along the flow direction. This phenomenon arises due to a larger distance between the shock layer and downstream window caused by outer shock waves. Consequently, this issue cannot be resolved solely through wall-based flow control measures; alternative flow strategies need to be explored to mitigate aero-optical interference resulting from this problem.

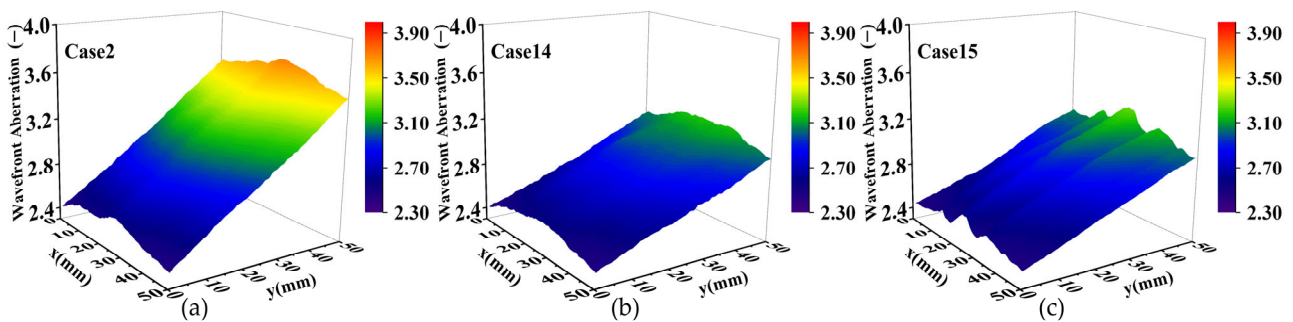


Figure 30. Comparison of optical wavefront aberration at $Ma = 3$, flow jet = 0.05 kg/s, $\theta = 15^\circ$: (a) without MVGs, without suction; (b) without MVGs, with suction; (c) with MVGs, with suction.

The following three comparison figures depict the wavefront distortion results after introducing inspiratory disturbance and with MVGs at 5 Ma, as illustrated in Figure 31. Incorporating inspiratory disturbance at high Mach numbers still effectively reduces the average wavefront distortion value. Furthermore, it can be observed from the comparative analysis that the inclusion of MVGs does not significantly mitigate aero-optics effects in the presence of inspiratory disturbance.

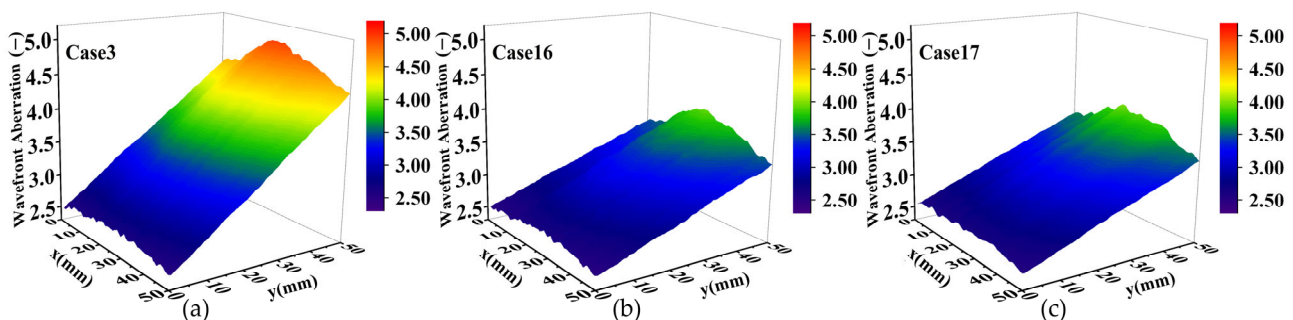


Figure 31. Comparison of optical wavefront aberration at $Ma = 5$, flow jet = 0.05 kg/s, $\theta = 15^\circ$: (a) without MVGs, without suction; (b) without MVGs, with suction; (c) with MVGs, with suction.

5. Conclusions

A comprehensive correction method is proposed for mitigating hypersonic aero-optical effects by adding MVGs and using blowing and suction flow control. Summarizing this work, the significant conclusions are drawn and listed as follows:

(1) This approach not only reduces the temperature of the window but also achieves a more uniform temperature distribution compared to the single jet cooling method, thereby minimizing aerodynamic thermal radiation effects.

(2) The temperature at the upstream position of the optical window is slightly higher with MVGs than without MVGs. The comparison of temperature distribution in the optical window at 5 Ma for jet cooling reveals that the inclusion of MVGs results in a more uniform temperature distribution across the entire window. This leads to a reduction in the temperature difference between the highest and lowest points, with only a 4.5 K variation observed. In contrast, when MVGs are not present, temperatures gradually increase along the flow direction, resulting in a significant temperature difference of 42.8 K.

(3) The inclusion of MVGs effectively mitigates large-scale structures generated by airflow passing through the rear step. As the Mach number increases, the wavefront distortion at 5 Ma can be reduced by 21.7%. Thus, increasing the Mach number enhances the contribution of MVGs in minimizing wavefront distortion.

(4) By implementing suction control at the lower step of the optical window, the refractive index fluctuations near the window are significantly reduced, leading to a more uniform surface temperature distribution. The application of this flow control technique results in a notable 14.7% reduction in wavefront distortion at 3 Ma and an approximate maximum value reduction of 20% at 5 Ma.

It can be concluded that combining MVGs with spray and suction controls ensures consistent side window temperatures, while maintaining flow field uniformity near the window during cooling processes, thus achieving the effective correction of hypersonic aero-optical effects.

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Nomenclature

DES	Detached Eddy Simulation
DDES	Delayed Detached Eddy Simulation
K_{GD}	Gladstone–Dale coefficient
k	turbulent kinetic energy
L_i	ray transmission distance in the i -th grid
Ma_∞	incoming Mach number
MVGs	micro-vortex generators
m_j	cooling gas jet rate
n	refractive index

n_i	refractive index of the fluid in the i -th grid
OPL	optical path length
OPD	optical path difference
$\langle OPL \rangle$	spatial average of the OPL s of all rays
OPD_{rms}	root mean square of the OPD s for all rays
P_0	incoming pressure
P_j	cooling jet pressure
T_0	static temperature
THAAD	Terminal High Altitude Area Defense
ν_t	moving eddy viscosity coefficient
$w(x, y, z)$	wavefront aberration at (x, y, z)
θ	angle between jet direction and seeker axis
ρ	density
ρ_{rms}	root mean square of the density
λ	wavelength of light
ω	specific dissipation rate
μ_t	dynamic turbulent eddy viscosity coefficient
ν	viscosity coefficient of moving molecules
κ	Karman constant
δ	recessed window depth
φ	angle of incidence of light

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