

Article

Integrated Design of Dual-Stage Inward-Turning Inlet and Modified Conical Forebody Considering Viscous Effects

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Abstract

To meet the integrated design requirements of the belly-mounted air-breathing vehicle forebody and inward-turning inlet, an integrated design method for the dual-stage compression inward-turning inlet (DCII) based on a conical forebody is developed, to facilitate subsequent splitter regulation. The parameters of a specific entrance in the forebody flowfield are adopted as the input parameters for the internal conical flowfield; combined with streamline tracing technology and the “dual-waverider” concept, an integrated configuration is generated. Numerical simulation results indicate that, under viscous conditions, the shock structure at the symmetry plane of Configuration 1 deviates from the design expectations. This discrepancy arises from the convergence of low-energy flow and the boundary layer. Configuration 2 was proposed by modifying the forebody shape to mitigate low-energy boundary layer flow convergence, achieving a mass flow coefficient of 0.95, a total pressure recovery coefficient of 0.51, and an outlet total pressure rise ratio of 26.14 at the design point ($Ma = 6.35$). At off-design points, Configuration 2 still maintains a high total pressure recovery coefficient while retaining both a high outlet pressure rise ratio and mass flow coefficient.

Keywords: conical inflow; inward-turning inlet; integrated design; dual-stage compression; streamline tracing

1. Introduction

The air-breathing combined-cycle propulsion vehicle is a research hotspot in the field of aerospace for the future [1–3]. Meanwhile, the integrated configuration of the belly-mounted air-breathing forebody/inlet can fully utilize the pre-compression capability of the forebody on the airflow, making it a typical aerodynamic layout for realizing the integrated airframe-propulsion design of vehicles. Compared with the traditional axisymmetric inlet [4] and two-dimensional inlet [5], the inward-turning inlet has advantages such as strong compression capacity and small wetted area, thus becoming the preferred internal flow design scheme for the integrated design. The curved cone forebody is widely adopted due to its advantages of simple structure, high volume ratio and excellent angle of attack characteristics [6].

At present, for the inlet of the combined-cycle propulsion during the wide-range adjustment process, the flow expansion caused by the excessive adjustment angle of the

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splitter will lead to a significant degradation in performance. Therefore, compared with the single-wave incidence inlet, the dual-wave incidence design exhibits remarkable advantages in suppressing flow expansion during the splitter plate adjustment [7]. The design concept of the XTER (Xiamen Turbine Ejector Ramjet) inward-turning combined inlet proposed by professor You [8] from Xiamen University utilizes the dual-wave incidence design to improve the aerodynamic performance of the inlet system. Liu [9] proposed a variable-geometry inlet design method incorporating an inward-turning inlet with an adjustable dual-stage compression surface, which provides an effective solution to the incompatibility issue between three-dimensional curved-surface compression and geometric configuration. In this design, the inward-turning inlet not only achieves high compression efficiency, but also reserves a large deflection angle for the splitter plate adjustment of the combined inlet.

In addition, the integrated design methods based on either the conical forebody or the forebody shock wave can both achieve the matching design of the forebody shock wave and the inlet lip [10–14]. However, due to the bending shock compression, the conical forebody will further aggravate the non-uniformity of the incoming flow at the inlet entrance [15]. The shock wave–boundary layer interaction generated between the low-energy boundary layer flow derived from the forebody and the shock wave will significantly degrade the inlet performance [16–18]. In particular, the lateral pressure gradient at the intersection line between the inlet and the forebody will intensify the accumulation of low-energy boundary layer flow toward the symmetry plane, thereby altering the local shock wave structure [19]. To solve this problem, one method is to remove the low-energy flow, and the other is to weaken the lateral pressure gradient. The bump-based integrated forebody/inlet configuration can effectively eliminate the low-energy flow originating from the forebody [20,21], yet the bump forebody is not conducive to reducing the lateral pressure gradient. In contrast, the conical forebody design for the High-speed Air-breathing Weapon Concept (HAWC) [22] proposed by Lockheed Martin Corporation is derived from the original scheme High-Speed Strike Weapon (HSSW) [23]. This optimized configuration not only balances the requirements of internal volume and lift-to-drag ratio, but also facilitates the improvement in the uniformity of the incoming flow at the inlet and the reduction in the lateral pressure gradient.

Accordingly, this study first constructs a novel dual-wave incident basic flowfield (DIBF) via the method of characteristics (MOC), which lays a favorable foundation for the subsequent splitter plate adjustment. Then, with the aim of attenuating the lateral pressure gradient of the aircraft conical forebody, the integrated design of the forebody/inlet is carried out by modifying the forebody profile and integrating it with the aforementioned DIBF. Subsequently, it analyzes the effects of the forebody on the shock structure of the inward-turning inlet. Finally, the flow characteristics and aerodynamic performance of the integrated configuration at both the design point and off-design point are analyzed through numerical simulation.

2. Methodology

2.1. Forebody Design

The design of the forebody geometry is illustrated in Figure 1. The forebody comprises an upper and a lower surface; the upper surface is a conical profile generated with a pointed von Kármán ogive *OAB* as the generatrix [24], and the lower surface is formed by the streamwise combination of a series of similar sectional profiles. The profile shape of the lower surface at the bottom cross-section obtained via the CST method is given as follows:

$$y(t) = C(t) \cdot S(t) \quad (1)$$

where $C(t)$ is the class function and $S(t)$ is the shape function. The class function $C(t)$ is given as follows:

$$C(t) = t^{N_1} \cdot (1 - t)^{N_2} \quad (2)$$

where t denotes the normalized coordinate. N_1 and N_2 represent the exponential parameters of the class function, which govern the characteristics of the trailing edge. For the shape design of vehicles, the fuselage cross-section is typically symmetric about the left–right axis, and thus N_1 is set equal to N_2 .

The shape function $S(t)$ is defined as follows:

$$S(t) = \sum_{k=0}^n c_k \cdot B_k^n(t), \quad B_k^n(t) = \binom{n}{k} t^k (1 - t)^{n-k}, \quad t \in [0, 1], \quad k = 0, 1, \dots, n \quad (3)$$

where c_k stands for the weight coefficient and $B_k^n(t)$ is the Bernstein polynomial. Therefore, the profile shape of the lower surface at the bottom cross-section is expressed as follows:

$$y(t) = t^{N_1} \cdot (1 - t)^{N_2} \cdot \sum_{k=0}^n c_k \cdot \binom{n}{k} t^k (1 - t)^{n-k}, \quad k = 0, 1, \dots, n \quad (4)$$

The lower surface shapes at different x -stations are derived by scaling this profile shape. Finally, two forebody configurations (Case 1 and 2) are derived by selecting appropriate parameters. The specific parameters are listed in Table 1. r denotes the radius of the bottom section circle of the upper surface, and d represents the maximum thickness of the bottom section profile curve of the lower surface, and L stands for the forebody length.

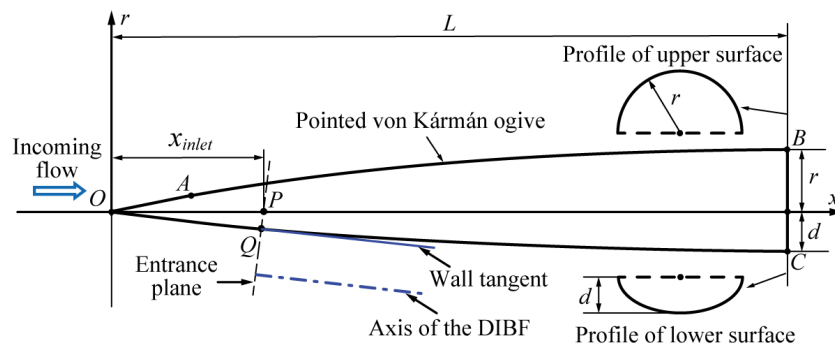


Figure 1. Design of conical forebody.

Table 1. Parameters of the forebody shapes.

Model	L/m	r/m	d/m	n	N_1	N_2	c_k
Case 1	10.0	1.0	1.00	2	0.5	0.5	(0.7, −0.25, 0.7)
Case 2			0.42		0.7	0.7	

2.2. Internal Conical Flowfield Design

2.2.1. Determination of Flowfield Input Parameters

For an outwardly convex forebody, wall airflow direction differs from the freestream, and deflection increases with convexity. Thus, airflow direction must be considered in subsequent inlet design. The relative positions of the forebody and the DIBF on the symmetry plane are shown in Figure 1. The line PQ is perpendicular to the wall at point Q , and x_{inlet} is the distance from the top vertex O to point P . The axis of the DIBF remains parallel to the tangent of the wall, ensuring that the basic flowfield aligns with the incoming airflow on the symmetry plane. The cross-section passing through point Q and perpendicular to the axis of the DIBF is defined as the entrance plane of the inlet. In the forebody flowfield, the post-wave flow parameters of this entrance are adopted as the

design input parameters for the internal conical flowfield. Ma_{entrance} represents the average Mach number on the entrance plane. For comparative analysis, with a specified value of x_{inlet} , the average Mach number at the entrance plane is obtained as 6.0 by iteratively solving the flow parameters. Therefore, under specific incoming flow conditions, the input parameters of the two cases of the internal conical basic flowfields remain consistent. The specific parameters are shown in Table 2.

Table 2. Incoming flow conditions and input parameters.

Model	H/km	$x_{\text{inlet}}/\text{m}$	Ma_{∞}/Ma	Ma_{entrance}/Ma
Case1	27.05	2.16	7.00	6.00
Case2			6.35	

2.2.2. Design of Dual-Wave Incident Basic Flowfield

The DIBF is modified from the original single-wave incident basic flowfield [25]. As shown in Figure 2, the axisymmetric generating body is composed of two quadratic curves, ab and bf . φ_a , φ_b and φ_f denote the wall inclination angles at the respective curve endpoints. Under the given incoming flow conditions, the shock compression region abe and the isentropic compression region bef are derived via the MOC. The wall inclination angle φ_a ensures the first conical shock wave hits the lip point e . The incoming flow conditions are given in Table 3. As shown in Figure 3, point c is selected on the curve bf , and a cubic curve cd is defined such that the shock wave generated from point c intersects the lip point e . At this position, φ_c denotes the inclination angle of curve cd , β is the shock angle of the second conical shock wave, δ is the deflection angle of airflow after the shock wave, and M_1 is the Mach number before the shock wave. The further forward point c is located, the smaller the shock wave angle becomes, and vice versa. When the shock angle β is too large, it is not conducive to the start of the inlet, so the position of point c needs to be determined by iteration. The expression of the polynomial curves and the relationship between the related parameters at point c satisfies Equations (5)–(8).

$$r_{ab} = k_1 x^2 + k_2 x + k_3 \quad (5)$$

$$r_{bc} = k_4 x^2 + k_5 x + k_6 \quad (6)$$

$$r_{cd} = k_7 x^3 + k_8 x^2 + k_9 x + k_{10} \quad (7)$$

$$\tan \delta = \frac{M_1^2 \sin^2 \beta - 1}{\left[M_1^2 \left(\frac{\gamma + 1}{2} - \sin^2 \beta + 1 \right) \right] \tan \beta} \quad (8)$$

where $k_1 \sim k_{10}$ are curve coefficients that can be adjusted as needed.

Table 3. Incoming flow conditions.

Ma_{∞}/Ma	P_{∞}/Pa	T_{∞}/K
6.00	1379.99	225.57

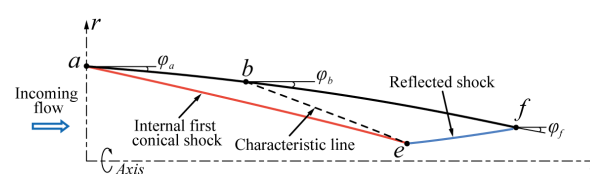


Figure 2. Design principle of single-wave incident basic flowfield.

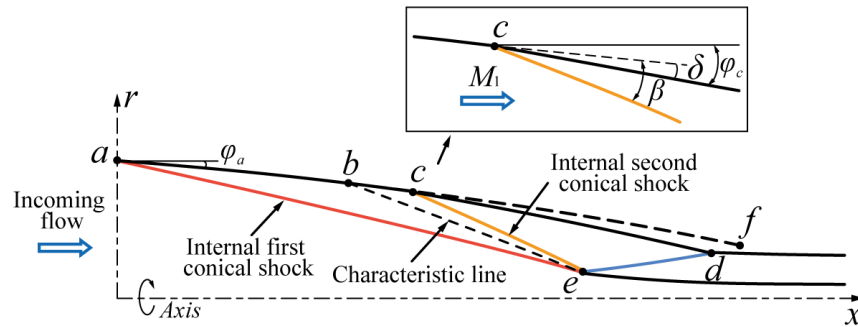


Figure 3. Design principle of dual-wave incident basic flowfield.

2.2.3. Optimization of Internal Conical Basic Flowfield

The DIBF plays a pivotal role in determining the performance of inlets. To ensure stable operation of the engine, the airflow at the exit should be as uniform as possible, while minimizing total pressure loss to the greatest extent possible. This part aims to enhance the inlet performance through multi-objective optimization, focusing on two key performance metrics of the basic flowfield downstream of the reflected shock wave: the mass flow-weighted standard deviation of the Mach number (D_{Ma}) and the total pressure recovery coefficient (σ). For the optimization, the Multi-Island Genetic Algorithm (MIGA) was adopted. For the multi-objective optimization problem, the Pareto optimization theory was introduced to obtain the optimal solution set on the Pareto frontier.

In the optimization process, the inclination angle φ_a and the coordinates of points a , b and c are adopted as optimization parameters, with the optimization model given as follows:

$$\left\{ \begin{array}{l} \text{opt} \left\{ \begin{array}{l} \max f_1 = \sigma \\ \min f_2 = D_{Ma} \end{array} \right. \\ \text{s. t.} \left\{ \begin{array}{l} \varphi_a \in V_1 \\ x_b \in V_2 \\ r_b \in V_3 \\ x_c \in V_4 \\ r_b \in V_5 \end{array} \right. \end{array} \right. \quad (3)$$

where V_i ($i = 1, 2, \dots, n$) denotes the optimization interval of the corresponding parameter, which is defined as a $\pm 10\%$ fluctuation around the parameter's initial value. Specifically, D_{Ma} quantifies the flowfield uniformity, calculated by a mass flow-weighted statistical method as follows:

$$D_{Ma} = \sqrt{\sum_{i=1}^n \left(Ma_i \frac{\dot{m}_i}{\dot{m}_{total}} - Ma^* \right)^2}, \quad Ma^* = \sum_{i=1}^n Ma_i \frac{\dot{m}_i}{\dot{m}_{total}} \quad (4)$$

where the subscript i ($i = 1, 2, \dots, n$) denotes the index of each unit grid element where the reflected shock wave of the base flowfield is located, $\dot{m}_i$ is the mass flow rate through the i -th grid element, $\dot{m}_{total}$ is the total mass flow rate, and Ma^* represents the mass-averaged Mach number.

Figure 4 shows the Pareto front of the σ and D_{Ma} . The basic flowfield corresponding to the Pareto front point is selected as the final optimization result.

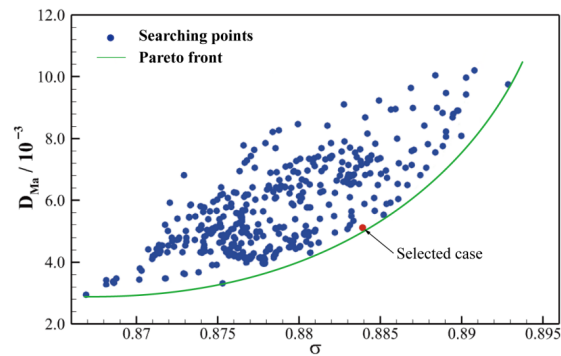


Figure 4. Pareto front of total pressure recovery coefficient and mass flow-weighted standard deviation of Mach number.

2.3. Matching Design

As illustrated in Figure 5, curve GH denotes the intersection line of the internal first conical shock wave and the conical forebody, and curve DEF represents the intersection line of the internal conical shock wave and the forebody shock wave. Combined with the inlet expansion angle θ , the inlet capture profile can be obtained. Figure 6 shows the schematic diagram of the shock wave structure, where the forebody shock wave o_1e_1 , the first internal conical shock wave a_1e_1 , and the second internal conical shock wave c_1e_1 all intersect at the inlet lip. Then, starting from the capture profile, the inward-turning inlet is generated in the DIBF by using streamline tracing technology. Since variations in the forebody's lower surface directly alter the shock wave position, it may be necessary to appropriately scale the DIBF during the design process in order to achieve the concept of “double wave” flow direction and ensure effective flow capture. Finally, Configuration 1 and 2 are generated for the two distinct forebody geometries (Case 1 and 2). As shown in Figure 7, the capture area (the red curve) is kept equal for the two configurations throughout the design process. The configuration parameters are presented in Table 4.

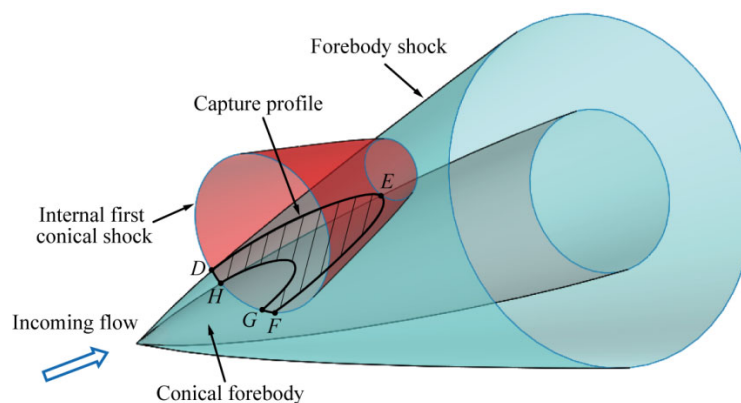


Figure 5. Determination of the capture profile.

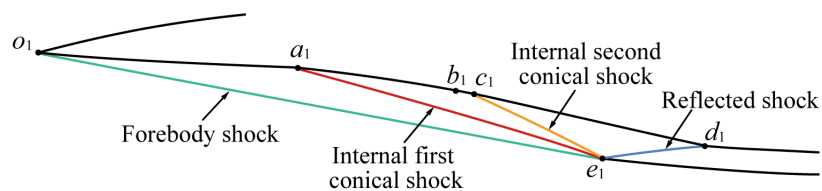


Figure 6. Schematic diagram of shock wave structure.

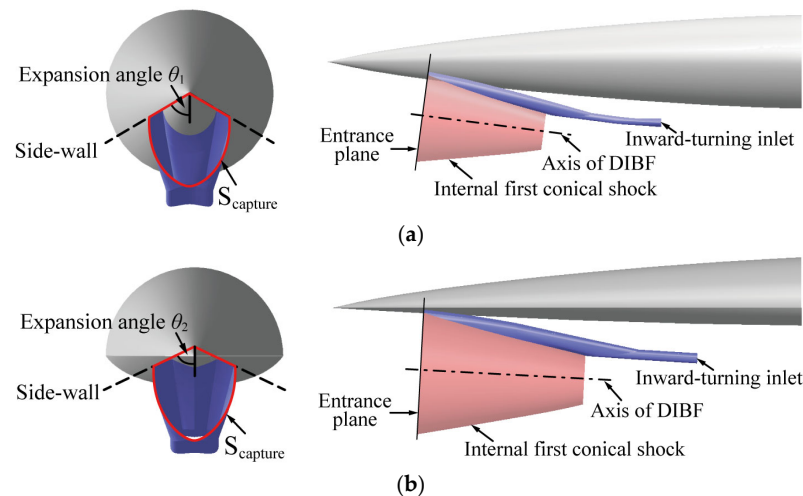


Figure 7. Integrated configuration generation (a) Configuration 1; (b) Configuration 2.

Table 4. Design parameters of the capture area.

	θ	$S_{\text{capture}} / \text{m}^2$
Configuration 1	$\theta_1 = 60^\circ$	$S_{1\text{capture}} = 0.754$
Configuration 2	$\theta_2 = 65^\circ$	$S_{2\text{capture}} = 0.754$

3. Numerical Method and Grid

3.1. Numerical Method and Verification

The numerical simulation solves the three-dimensional Reynolds-averaged Navier–Stokes equations based on density, with the $k-\omega$ SST turbulence model employed. Inviscid convective fluxes are computed using the second-order upwind Roe FDS scheme. Air is treated as an ideal gas, and the molecular viscosity is derived from the Sutherland formula. Adiabatic, no-slip, and solid wall boundary conditions are applied throughout the calculation. The computational stand is a self-developed computational hydromechanics numerical simulation software NNW-Flowstar_V1.4 [26], which has undergone a large number of engineering tests.

To verify the reliability of the employed computational approach, a double-ellipsoid model from the literature [27] was adopted, as depicted in Figure 8. Pressure measurement experiments for this model were carried out in the FD-07 blowdown high-Mach wind tunnel at the Beijing Institute of Aeronautics and Astronautics. Relevant test parameters are listed in Table 5, where Re represents the unit Reynolds number, P_0 denotes the total pressure, T_0 stands for the total temperature, and α is the angle of attack. Figure 9 presents the pressure coefficient distributions along the upper surface derived from both numerical simulations and experimental measurements. The numerical results are in good agreement with the experimental data, thereby confirming the effectiveness of the adopted numerical simulation method.

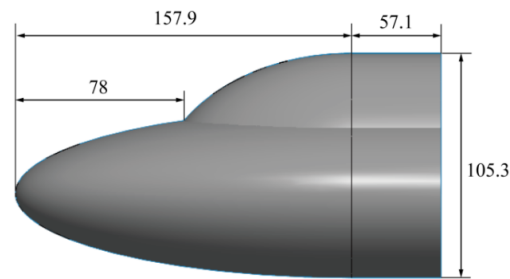


Figure 8. Schematic diagram of double-ellipsoid model (unit: mm).

Table 5. Experimental parameters of the double-ellipsoid model.

Ma_{∞}/Ma	Re	P_0/MPa	T_0/K	$\alpha/^\circ$
8.02	1.98×10^7	8.5	720	0

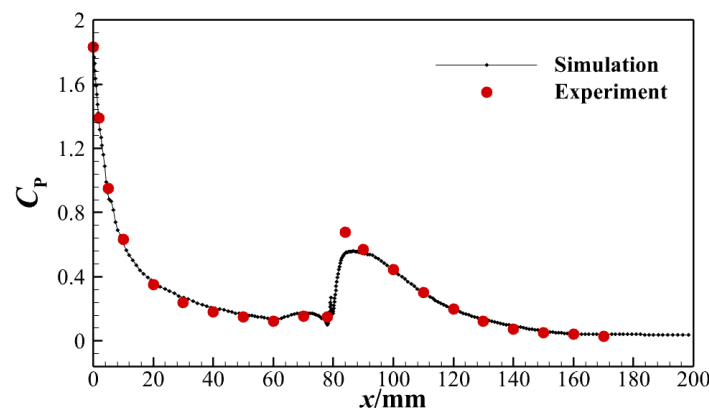


Figure 9. Comparison between calculated and experimental results of pressure distribution along the upper surface.

3.2. Grid and Computational Domain

Figure 10 depicts the computational domain, grid, and boundary conditions of the integrated configuration. A pressure outlet boundary condition is assigned to the exit, an adiabatic no-slip solid boundary condition to the gray region, and a symmetric plane boundary condition to the blue region. The parameters for setting farfield conditions are shown in Table 6. The integrated configurations proposed in this study have analogous geometries under different operating conditions. Accordingly, grid independence verification is only conducted for Configuration 1. Three grid scales (coarse, medium, and fine) are generated, with the height of the first mesh layer satisfying $y^+ \leq 1$. The calculated results for different grid scales are presented in Table 7, which indicates that minor discrepancies exist among the results across various grid scales. Additionally, Figure 11 presents the pressure distributions on the lower surface of the forebody and upper surface of the inward-turning inlet at the symmetric plane for different grid scales. Comparisons demonstrate consistent trends in wall pressure rise with x and negligible differences. However, the medium and fine grids can accurately capture the peaks of pressure variations, whereas the coarse grid exhibits insufficient precision in this regard. Consequently, considering both computational accuracy and efficiency, the medium-scale grid is selected for the numerical simulation and analysis of the generated series of integrated configurations.

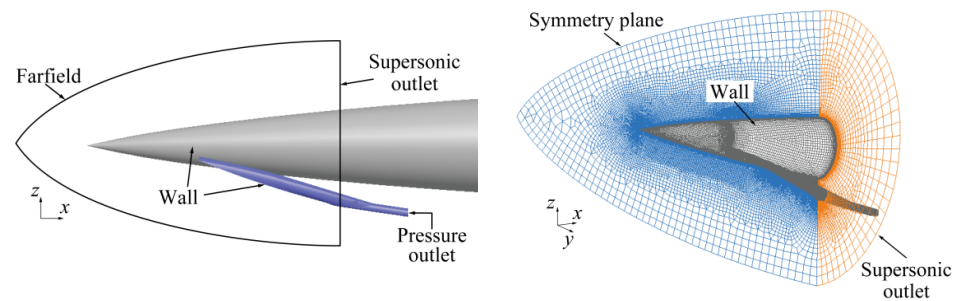


Figure 10. Computational domain, grid, and boundary conditions of the integrated configuration.

Table 6. Farfield boundary condition.

H/km	Ma _∞ /Ma	P _∞ /Pa	T _∞ /K
29.05	7.00	1379.99	225.57

Table 7. Calculation results of different scales at Ma = 7.0.

Model	Grid Size	σ _{exit}	Ma _{exit}
Coarse	6,174,930	0.574	3.118
Medium	10,451,610	0.580	3.102
Fine	21,967,240	0.583	3.095

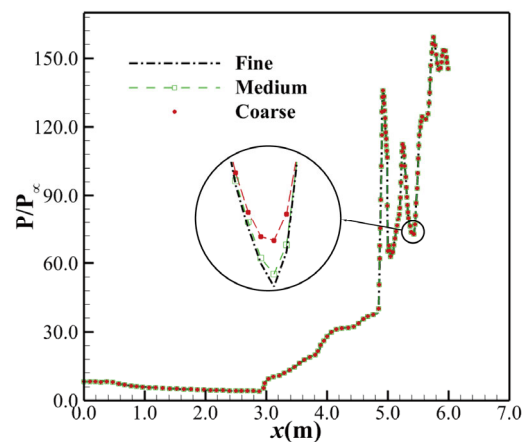


Figure 11. Comparison of pressure rise along the symmetry plane with different scales of grids.

4. Result Analysis

4.1. Design Verification of Dual-Stage Compression Inward-Turning Inlet

Then, the dual-stage compression inward-turning inlet (DCII) is generated from the capture profile via streamline tracing within the DIBF. Under viscous conditions, the Mach number distribution on the symmetry plane of DCII at the design point is shown in Figure 12, while the MOC results for the basic flowfield are presented in Figure 13. Overall, the numerical simulation results are in good agreement with the MOC results. The simulation results indicate that the Mach number distribution downstream of the reflected shock wave is relatively uniform. Meanwhile, the dual-stage incident shock system maintains an expected wave structure. The internal first and second conical shock waves along the flow direction are clear and hit at the lip. The reflected shock is obviously weakened after hitting near the shoulder point, but it cannot be completely attenuated due to the influence of viscosity. Table 8 presents the performance of the DCII, including

the freestream Mach number Ma_∞ , the mass flow coefficient ϕ , the exit Mach number Ma_{exit} , the pressure raise ratio π , and the total pressure recovery coefficient σ .

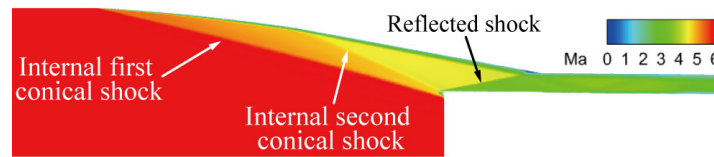


Figure 12. Mach number distribution on the symmetry plane.

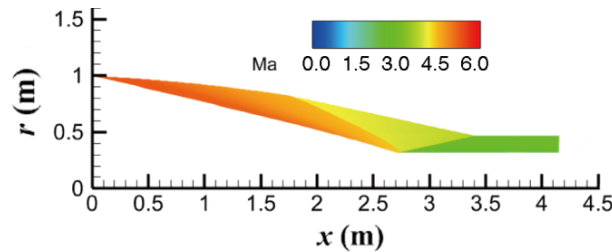


Figure 13. The MOC results of dual-wave incident basic flowfield.

Table 8. Performance parameters of DCII configuration.

Model	Ma_∞	Ma_{exit}	ϕ	σ	π
DCII	6.0	2.75	0.95	0.473	21.20

4.2. Integrated Configuration Simulation Results and Performance Analysis

4.2.1. At Design Point

Figure 14a shows the density gradient on the symmetry plane of Configuration 1. While the forebody shock properly hits the inlet lip as designed, the internal shock structure manifests a significant departure from the anticipated dual-stage compression scheme. Figure 14b provides the Mach isoclines across sections along the flow direction, showing that the conical shock wave maintains good attachment to the inlet leading edge with clear structure in the spanwise direction, which is consistent with the original design intent. The internal conical shock and the boundary layer developed from the forebody interfere, causing significant deformation at the root of the internal conical shock and leading to flow separation. Additionally, due to the strong lateral pressure gradient near the intersection line, the low-energy flow from the forebody converges on the symmetry plane, forming a passage vortex extending downstream, as shown in Figure 14c. Analysis shows that the viscous condition might be the reason for the difference between the shock wave structure and the original design. Figure 15 presents the density gradient on the symmetry plane of Configuration 2, revealing that the forebody shock hits the lip, and the dual-stage incident shock structure is clearly consistent with the design intent. The region with a total pressure recovery coefficient less than 0.25 is defined as low-energy flow [16].

The throat height ratio of low-energy flow is the ratio of low-energy flow thickness to throat height, defined as follows:

$$\eta = \frac{h_{\text{low-energy}}}{H_{\text{cowl}}} \quad (5)$$

where $h_{\text{low-energy}}$ denotes the height of the low-energy flow, and H_{cowl} the height of the throat. Figure 16 presents the low-energy flow distribution at the lip section A–A and throat section B–B, demonstrating that Configuration 2 features a low-energy flow-to-throat height ratio of 26.5%, which represents a significant reduction from the 79.2% ratio

for Configuration 1. Furthermore, as shown in Figure 17, the Mach number distribution of the throat section B–B of Configuration 2 on the symmetry plane is more uniform than that of Configuration 1.

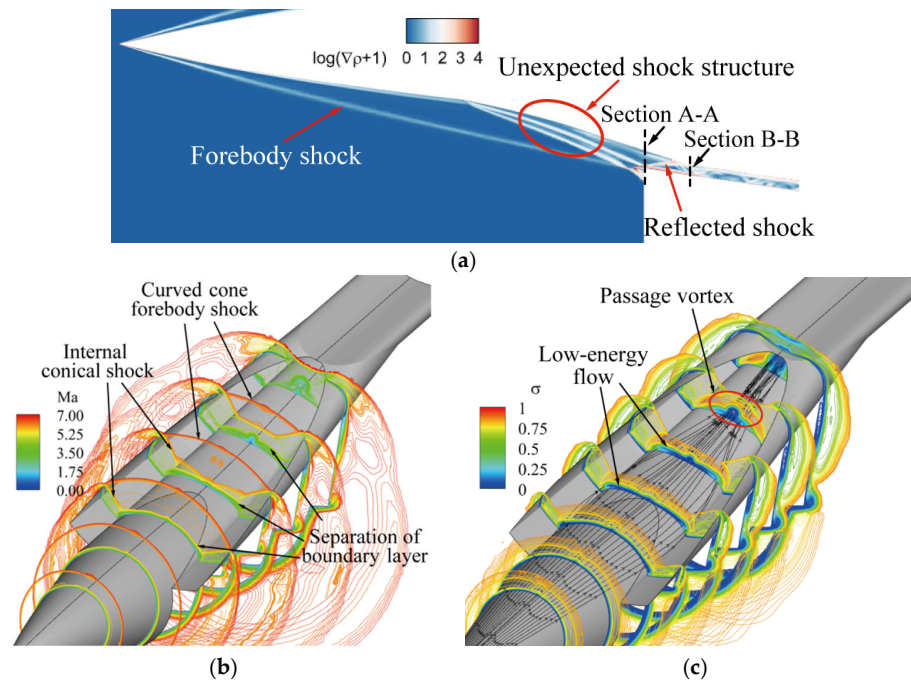


Figure 14. Configuration 1: (a) Density gradient of symmetry plane under viscous condition; (b) Mach isoclines of cross-sections along flow direction; (c) low-energy flow development along flow direction.

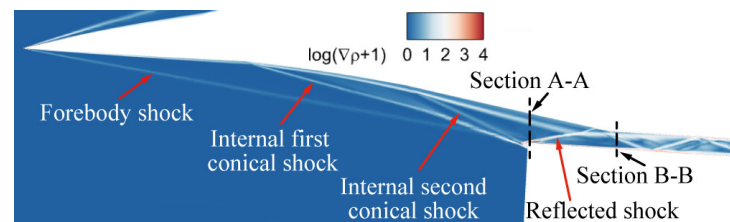


Figure 15. Density gradient of symmetry plane under viscous condition (Configuration 2).

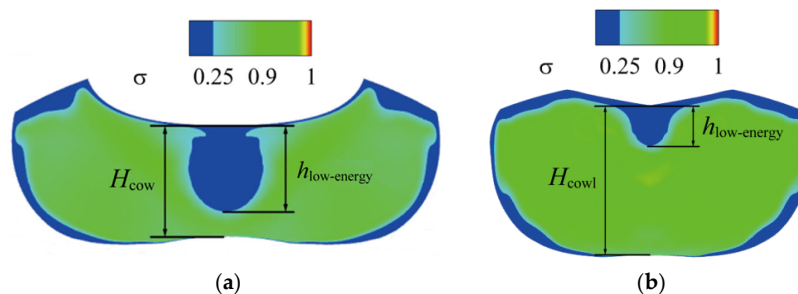


Figure 16. Low-energy flow area (section A–A): (a) Configuration 1; (b) Configuration 2.

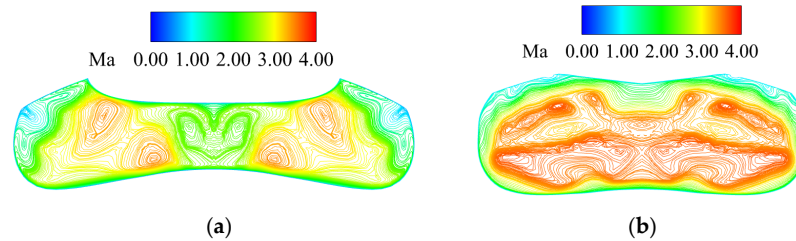


Figure 17. Mach number distribution of throat section (section B-B): (a) Configuration 1; (b) Configuration 2.

As shown in Table 9, two configurations maintain excellent flow capture capability. Specifically, compared to Configuration 1, Configuration 2 achieves a total pressure recovery coefficient of 0.51. This improvement is attributed to the forebody design, which enhances inlet flow quality by reducing lateral pressure gradients near the intersection line, thus effectively weakening the channel vortex intensity. However, the forebody reduces the deceleration and pressurization effect on the airflow, leading to an increase in the Mach number at the exit and a decrease in the pressure rise ratio.

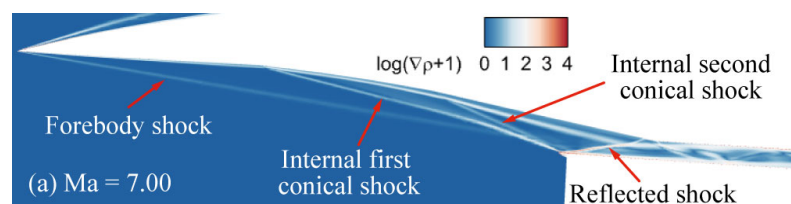
Table 9. Performance of integrated configuration at design point under viscous conditions.

Model	Ma_∞	Ma_{entrance}	Ma_{exit}	ϕ	σ	π
Configuration 1	7.00	6.00	2.50	0.93	0.33	56.70
Configuration 2	6.35	6.00	2.90	0.95	0.51	26.14

4.2.2. At Off-Design Point

This part conducts numerical simulations of Configuration 2 under off-design point $Ma = 5.50$, and $Ma = 7.00$, with the incoming flow angle $\alpha = 0^\circ$. Figure 18 presents the density gradient distribution on the symmetry plane. The flowfield structures at the off-design point share similar characteristics with those at the design point, with a forebody shock wave and two incident internal conical shock waves observed in both cases. At off-design point ($Ma = 7.00$) exceeding the design Mach number ($Ma = 6.35$), the forebody shock interacts with the internal first conical shock. Nevertheless, due to the weak intensity of the forebody shock, this intersection produces negligible flow disturbances.

Thus, the shock wave impinges on the interior of the inlet with a flow coefficient of 1.0. Figure 19 illustrates the performance of Configuration 2 under typical freestream conditions. When the inflow Mach number falls below the design condition ($Ma < 6.35$), the forebody shock progressively detaches from the inlet lip, resulting in a corresponding reduction in the mass flow coefficient. Nevertheless, the internal dual-stage shock compression structure remains fully preserved within the inlet. Studies have shown that Configuration 2 maintains a high total pressure recovery coefficient while maintaining a high pressure rise ratio.



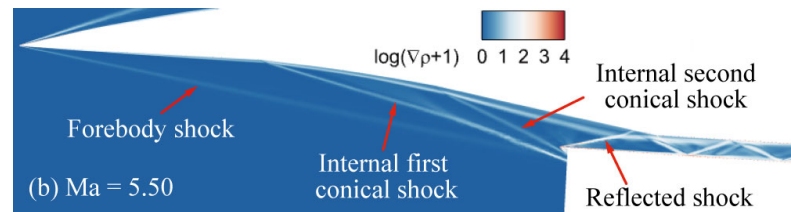


Figure 18. Density gradient at symmetry plane of Configuration 2 at different Mach.

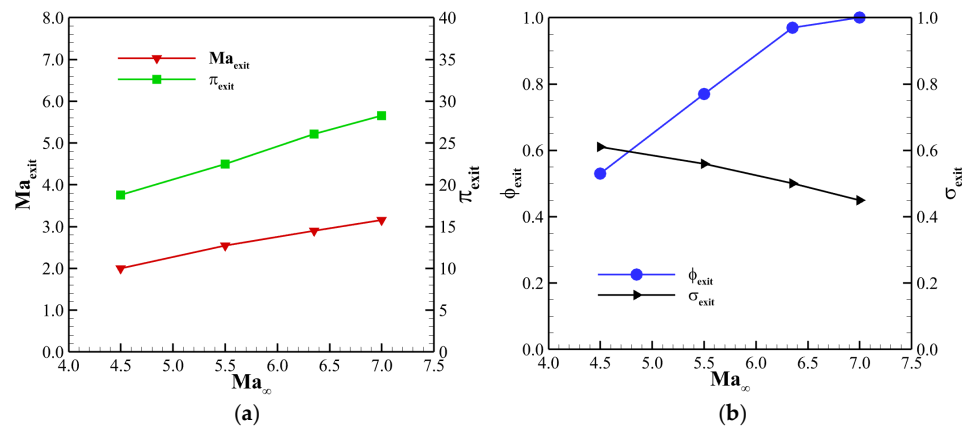


Figure 19. Variation in exit performance with incoming Mach number: (a) Ma_{exit} and π_{exit} ; (b) ϕ_{exit} and σ_{exit} .

5. Conclusions

In this paper, a novel dual-wave incident basic flowfield (DIBF) via the MOC is constructed, and an integrated design method of DCII based on DIBF is developed. Through this method, two integrated configurations (Configuration 1 and 2) are obtained, with numerical simulations subsequently carried out. Numerical simulation results show that this method is feasible, providing a new approach for the integrated design of conical forebody and dual-stage inward-turning inlet. The DCII designed based on a DIBF exhibits a total pressure recovery coefficient of 0.473, an exit Mach number of 2.75, and an exit pressure rise ratio of 21.20, under viscous conditions at the design point ($Ma = 6.00$). As for the integrated configuration, under viscous conditions, the shock structure at the symmetry plane of Configuration 1 differs from the design expectation, due to the convergence of low-energy flow at the symmetry plane and its interaction with the boundary layer. Configuration 2 was proposed by modifying the forebody shape to mitigate low-energy boundary layer flow convergence, achieving a mass flow coefficient of 0.95, a total pressure recovery coefficient of 0.51, and an outlet total pressure rise ratio of 26.14 at the design point ($Ma = 6.35$). At off-design points, Configuration 2 still maintains a high total pressure recovery coefficient while retaining both a high outlet pressure rise ratio and mass flow coefficient. The total pressure recovery coefficients at $Ma = 4.50$, $Ma = 5.00$ and $Ma = 7.00$ reach 0.61, 0.56 and 0.45 respectively, and the outlet pressure rise ratio was 18.78, 22.46 and 28.30.

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