

Article

Modeling the Mechanical Erosion of C/C-SiC Composites Under Dense Particle Impacts

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Abstract

Advancements in solid rocket propulsion have intensified the demand for higher specific impulse. Incorporating high-density, high-calorific metal additives into propellant formulations offers a viable pathway to meet these performance targets. Nevertheless, the ablation response of C/C-SiC thermal protection systems (TPS) exposed to high-temperature, high-velocity, dense gas-particle flows remains inadequately characterized. To address this, we employ high-fidelity numerical simulations to resolve the gas-side ablation behavior under extreme multiphase conditions. By calibrating against experimental data, we quantify the erosive mechanisms induced by boron-laden exhaust plumes and derive an empirical correlation for the linear ablation rate. This model serves as a predictive tool for TPS design in severe operational environments. Parametric investigations were conducted across three key variables: particle diameter (30–50 μm), condensed-phase mass loading (28–68%), and impact angle (18–27°). Validation against ground testing yielded a mean deviation of 3.87%, confirming its applicability to the conditions studied in this paper. Results indicate a positive correlation between ablation rate and particle concentration, impact velocity, incident angle (within the tested range), and particle size. During sensitivity analyses, a single-variable control protocol was enforced to isolate individual parameter effects.

Keywords: solid rocket motor; C/C-SiC; ablation; two-phase flow; Oka model

1. Introduction

During the operation of a solid rocket motor, critical thermal protection components—including the nozzle convergent section, throat, combustion chamber liner, and insulation layers—are subjected to extreme environments characterized by high temperatures, high velocities, and the scouring of dense condensed-phase particles. This inevitably induces material ablation, leading to mass loss and structural degradation that compromise the operational reliability and service life of the propulsion system [1–6]. Consequently, elucidating the ablation kinetics of these materials under actual service conditions remains a central research objective.

C/C-SiC composites are the preferred material for such applications due to their exceptional ablation resistance [7,8] and the formation of a passive oxide layer in high-temperature aerobic environments [9,10]. The synergistic combination of carbon fiber



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reinforcement (high specific strength) and the SiC ceramic matrix (high-temperature hardness) ensures dimensional stability under extreme conditions [11]. Current consensus in the academic community holds that C/C-SiC ablation is governed by the coupled mechanisms of thermochemical reaction and mechanical erosion [12–15], with the latter often dominating in particle-laden flows. For instance, Wang et al. [16,17] demonstrated that mechanical erosion causes more severe degradation in flexural and shear strength compared to pure thermochemical oxidation. Similarly, Gao et al. [9] revealed a transition in failure modes: initial thermochemical effects give way to mechanically eroded-dominated processes in later stages.

Parametric studies have identified key variables influencing erosion rates. Ragav [18] and Bolin et al. [19] emphasized the roles of impact angle, condensed-phase mass loading, and particle diameter. Notably, Al-Haidari [20] investigated the non-monotonic relationship between particle diameter and erosion rate, identifying a critical diameter that maximizes the material removal rate. Furthermore, Ben-Ami and Hu [21,22] established the non-linear dependence of erosion rate on impact angle, noting the existence of an optimal incidence angle for peak ablation. While these experimental insights are invaluable, they are often constrained by instrumental limitations and high costs, precluding extensive repetitive testing.

Numerical simulation offers a complementary approach to deconvolve these coupled physical phenomena. Compared to physical testing, simulations provide full-field data, facilitating the understanding of subsurface damage mechanisms and shortening development cycles. For C/C-SiC composites specifically, Wei et al. [23] successfully employed multi-physics models to reveal zoned ablation mechanisms. Regarding particle erosion algorithms, classical models such as Finnie [24–26], McLaury [27], and Oka [28,29] are widely used. However, the Finnie and McLaury models exhibit limitations under extreme hypersonic conditions involving large impact angles and high enthalpy. Comparative studies by Wen [30] and Ni [31] consistently demonstrate that the Oka model provides superior predictive accuracy for C/C-SiC composites subjected to complex fluid-thermal interactions.

Despite these advances, a critical gap remains in the existing literature: most studies isolate the effects of temperature, velocity, or particle concentration, whereas research addressing the combined effects of high temperature, high velocity, and high-concentration particulate flows is notably lacking. To bridge this gap, this study employs the Oka model to simulate C/C-SiC ablation under fully coupled extreme conditions. By systematically varying the particle impact angle (18–27°), condensed-phase mass fraction (28–68%), and particle diameter (30–50 µm), the individual contributions of these parameters are quantified. Finally, an empirical correlation for the linear ablation rate is derived, providing a reliable predictive tool for the design of thermal protection systems.

2. Numerical Methods and Experimental Setup

2.1. Simulation Model

During the operation of a solid rocket motor, the flow field inside the combustion chamber involves complex three-dimensional, two-phase flow, with the two-phase combustion gases exhibiting significant turbulent characteristics [32]. Due to turbulent diffusion, alumina droplets collide with the walls, resulting in highly intricate droplet trajectories. Simultaneously, the interaction between the gaseous and condensed phases within the two-phase combustion gases exerts a complex influence on the internal flow field. Therefore, selecting an appropriate turbulence model is crucial for accurately calculating the heat transfer between the two-phase combustion gases and the inner wall of the combustion chamber.

In this study, the Oka model is coupled with the k - ϵ turbulence model for the calculations. First, the k - ϵ equations are solved to obtain the gas-phase velocity field and

turbulent viscosity distribution. Then, based on the gas-phase velocity field, the Lagrangian method is used to track the trajectories of discrete-phase particles, and the particle impact velocity and angle at the wall are calculated. Finally, these parameters are substituted into the Oka model to determine the wall erosion rate. Specifically, the gas-phase governing equations are discretized using the Finite Volume Method (FVM), where the pressure-velocity coupling is handled by the SIMPLEC algorithm, diffusion terms are discretized with second-order central differencing, and convective terms employ a second-order upwind scheme. Furthermore, the trajectories of discrete-phase particles are solved using the Runge–Kutta method. All calculations are performed using ANSYS 2024 R2 Fluent.

All equations in this study are formulated in the Cartesian coordinate system, and the standard k - ε turbulence model is adopted. Renowned for its stability, computational efficiency, and reasonable accuracy, the standard k - ε model is one of the most widely used and mature turbulence models [33]. It assumes that the flow is fully turbulent and neglects molecular viscosity effects, making it highly suitable for simulating the turbulent flow field in this study. The transport equations for turbulent kinetic energy k and dissipation rate ε are given as follows:

$$\frac{\partial}{\partial t}(\rho k) + \frac{\partial}{\partial X_i}(\rho k u_i) = \frac{\partial}{\partial X_j} \left[\left(\mu + \frac{\mu_t}{\sigma_k} \right) \frac{\partial k}{\partial X_j} \right] + G_k + G_b - \rho \varepsilon - Y_M + S_k \quad (1)$$

$$\frac{\partial}{\partial t}(\rho \varepsilon) + \frac{\partial}{\partial X_i}(\rho \varepsilon u_i) = \frac{\partial}{\partial X_j} \left[\left(\mu + \frac{\mu_t}{\sigma_\varepsilon} \right) \frac{\partial \varepsilon}{\partial X_j} \right] + C_{1\varepsilon} \frac{\varepsilon}{k} (G_k + C_{3\varepsilon} G_b) - C_{2\varepsilon} \rho \frac{\varepsilon^2}{k} + S_\varepsilon \quad (2)$$

The formula is expressed using the Einstein summation convention, where X_i and X_j are spatial coordinate components, and $i, j = 1, 2, 3$ correspond to the x, y , and z directions, respectively. ρ is the gas-phase density; u_i is the gas-phase mean velocity component; G_k represents the turbulent kinetic energy generated by the velocity gradient of the laminar flow; G_b is the turbulent kinetic energy generated by buoyancy variations; Y_M denotes the fluctuation caused by expansion dissipation in compressible turbulence; $C_{1\varepsilon}$, $C_{2\varepsilon}$, and $C_{3\varepsilon}$ are constants; σ_k and σ_ε are the turbulent Prandtl numbers for the k and ε equations, respectively; and S_k and S_ε represent user-defined source terms. The unknowns of this set of equations are k (turbulent kinetic energy), ε (dissipation rate), and the gas-phase mean velocity field u_i , while the density field is calculated by coupling with the ideal gas state equation.

Currently, in most numerical simulations of two-phase flow and heat transfer in solid rocket motor internal flow fields, condensed-phase particles such as alumina and boron oxide are treated as inert particles. This approach effectively captures the realistic behavior of two-phase flow within the internal flow field.

In summary, the total acceleration of a fluid particle in the Eulerian framework can be expressed as:

$$a = \frac{dV}{dt} = \frac{\partial V}{\partial t} + u \frac{\partial V(M, t)}{\partial x} + v \frac{\partial V(M, t)}{\partial y} + w \frac{\partial V(M, t)}{\partial z} = \frac{\partial V}{\partial t} + (V \cdot \nabla) V \quad (3)$$

In the equation, a is the particle acceleration; V is the particle velocity vector; dV/dt is the total derivative of the particle velocity with respect to time; $\partial V/\partial t$ is the local acceleration of the particle; x, y, z are the spatial coordinates of the particle; t is time; and u, v, w are the components of the particle velocity vector in the x, y , and z directions, respectively. Equation (3) represents the material derivative of the gas-phase velocity within the Eulerian framework. It is used to calculate the gas-phase drag force acting on the discrete-phase particles and serves as a core input for solving the particle trajectories.

Under the experimental conditions of this study, the condensed-phase particles in the solid rocket motor primarily consist of boron combustion products such as B_2O_3 and B_4C , along with a small amount of other combustion reaction particles like Al_2O_3 . Measurements of particle diameters collected by water-cooled probes at the exit of the engine's afterburner chamber reveal that the granularity of condensed-phase combustion products typically exhibits a trimodal distribution. As the length of the afterburner chamber duct decreases, the particle diameter distribution tends to shift toward the peak of the maximum particle size, primarily concentrated around 40 micrometers [34].

In this study, the operating condition at the nozzle exit is approximately 1000 K, at which the condensed-phase particles of B_2O_3 exist in a liquid state. In their studies under similar operating conditions, Wen and Chen [31,35] both assumed the liquid particles to be solid, thereby employing the Oka model to calculate particle erosion. Wen's research compared the simulated maximum erosion rates and found that the simulation results were on the higher side, with a relative error of 13.89%. Similarly, Chen's study also found that the simulated maximum erosion rates were overestimated, with an error of 7.95%. It can be observed that the error resulting from simplifying all particles as solid for calculation falls within an acceptable range. Therefore, this paper also assumes that all particles are solid, neglecting the surface tension and adhesion effects after high-temperature melting, and uses the Oka model to calculate particle erosion. However, under operating conditions where the condensed-phase mass fraction is $>70\%$, the particle agglomeration effect may be enhanced, and this simplification might slightly overestimate the erosion rate. Future research will introduce a liquid-solid two-phase particle coupling model for correction. In the simulation, the physical properties of the various particles were normalized based on their mass fractions, treating them as a single particle type for computational analysis, while factors such as inter-particle collisions, agglomeration, or breakup were ignored. The normalized physical parameters of the particles are shown in Table 1.

Table 1. Partial Physical Parameters After Normalization.

Physical Property Parameter Names	Unit	Value
Density	kg/m^3	2518.8
Specific heat	$J/(kg \cdot K)$	871

The normalized density and specific heat are calculated as mass-fraction-weighted averages of the respective condensed-phase components, with the mass fractions for each material provided in Table 2.

Table 2. Mass fraction of condensed-phase particles in two-phase flow.

Component	Mass Fraction
C	0.100
B_2O_3	0.100
BN	0.070
B_4C	0.336
Al_2O_3	0.074
Total	0.680

In this study, the Oka model is employed to calculate wall erosion. The Oka erosion model is a predictive framework for estimating material ablation caused by particle impact, originally proposed by Japanese scholars Oka et al. [28,29]. In ablation simulations,

particularly when addressing complex fluid-thermal coupling problems, it can enhance computational efficiency and accuracy.

The Oka model provides the relationship of the erosion ratio e_r with respect to material properties and particle impact angle.

$$e_r = g(\alpha)e_{ref} \left(\frac{U}{U_{ref}} \right)^{k_1} \left(\frac{D}{D_{ref}} \right)^{k_2} \quad (4)$$

In the equation, U_{ref} and D_{ref} are the particle velocity and diameter under reference conditions, respectively; e_{ref} is the erosion rate under normal incidence; $g(\alpha)$ is the relationship describing how erosion varies with angle, given by the following expression:

$$g(\alpha) = (\sin \alpha)^{n_1} [1 + HV(1 - \sin \alpha)]^{n_2} \quad (5)$$

α is the angle between the particle velocity vector and the wall tangent plane; HV is the Vickers hardness (GPa).

During the calculation, the velocity vector of each particle was tracked, and the impact angle of each particle was calculated individually upon its impact with the wall. Then, the Oka model was applied to obtain the erosion rate of individual particles. Finally, the erosion rates of particles impacting the same wall mesh cell were summed to obtain the final erosion rate result.

2.2. Ground Test Facilities Mesh Model and Boundary Conditions

Ground ablation experiments were conducted based on the key performance parameters of the oxygen-lean propellant used in the simulation study, as well as the dimensional conditions of components such as the combustion chamber, nozzle, and bend pipe. The ground test setup is shown in Figure 1a. The experimental propellant used a boron-containing propellant produced by the Hubei Institute of Aerospace Chemotechnology. The propellant composition enters from the inlet of the gas chamber, passes through the contraction section to the throat, and then enters the expansion section, with the airflow continuously accelerating throughout this process. Influenced by the contraction section, the particles form high-density particle clusters under the action of the airflow and converge along the axis region of the combustion chamber. After exiting the expansion section, they impact the upper wall surface of the bend transition section at a specific angle and velocity. In view of the complex geometric features of the combustion chamber and elbow components, an O-grid cutting method was adopted for the numerical simulation mesh to generate a fully hexahedral structured grid.

Refer to Table 3 for the propellant performance parameters. Where AP is Ammonium Perchlorate, HTPB is Hydroxyl-terminated polybutadiene.

Table 3. Key Performance Parameters of Oxygen-Lean Propellant.

No.	Item	Parameter
1	Calorific value	<45 MJ/L
2	Propellant composition (mass fraction)	B (0.34), AP (0.33), HTPB (0.26), Al (0.04), Mg (0.03)
3	Gas molecular weight	11.83 g/mol
4	Specific heat at constant pressure	1.16 kJ/kg·K
5	Gas temperature	≈ 1860 K
6	Gas-phase particulate content	≈ 0.68

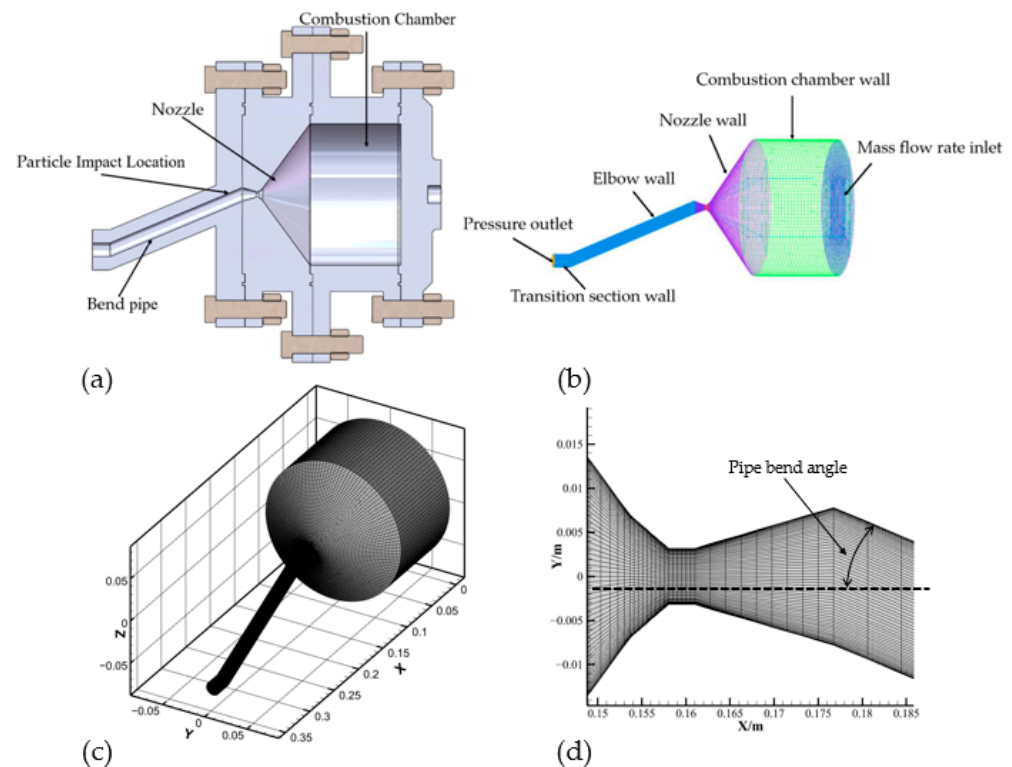


Figure 1. Schematic Diagram of the Ground Test Setup and Computational Mesh: (a) ground test setup, (b) computational mesh, (c) mesh size diagram, (d) close-up view of the mesh at the throat.

The overall configuration and mesh generation of the internal flow field are shown in Figure 1b. Since the internal flow field has a relatively regular shape, using a hexahedral structured mesh results in good mesh quality and effectively reduces the total number of grid cells. Additionally, fillets have been applied to the connection areas at transitional regions. The mesh size in this study is at the millimeter level, which is significantly larger than the particle diameter (30–50 μm). Consequently, the particle motion is not affected by the mesh scale. The discrete-phase calculation adopts the Lagrangian point-particle assumption, eliminating the need to consider mesh-particle size coupling effects.

The boundary conditions for this numerical simulation are set as follows:

- (1) At the inlet, a mixed boundary condition is applied. Specifically, the total temperature, turbulent kinetic energy, and dissipation rate are set using Dirichlet boundary conditions, while the velocity components are determined jointly by flux constraints and interior field extrapolation, which constitutes a mixed boundary condition. The static pressure is specified using a Neumann boundary condition. The inlet mass flow rate is 0.2192 kg/s, the total temperature is 1860 K, and the initial reference pressure is 5 MPa.
- (2) At the outlet, a mixed boundary condition is also applied. Specifically, the static pressure is set using a Dirichlet boundary condition, while the remaining variables are specified using Neumann boundary conditions. The outlet pressure is 101,325 Pa, and the total temperature is 300 K.
- (3) The wall is set as a no-slip adiabatic boundary.
- (4) The discrete-phase boundary condition for condensed-phase particles at the wall is set to “reflect”, with an average particle diameter of 40 μm .

Based on the numerical simulation method, a mesh independence study was conducted using the gaseous phase conditions as an example. The particle impact velocity at

the wall was selected as the validation parameter. The results for the five different mesh resolutions are shown in Figure 2 and Table 4.

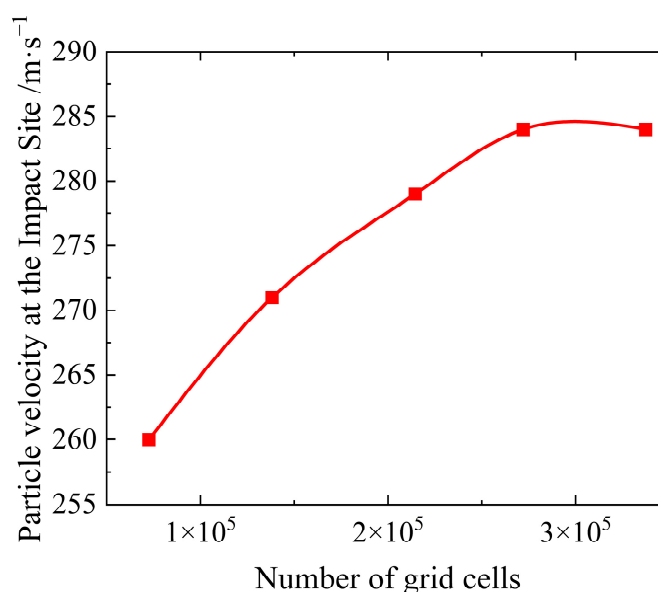


Figure 2. Line Plot of Mesh Independence Study Results.

Table 4. Mesh Independence Study Results.

Number of Grid Cells	Particle Diameter/ μm	Particle Velocity at the IMPACT Site/ $\text{m}\cdot\text{s}^{-1}$	Comparison of Particle Velocity at the Impact Site
72,477	40	260	92%
138,147	40	271	95%
214,501	40	279	98%
272,075	40	284	100%
337,363	40	284	100%

The comparison results show that the calculated results converge to a constant value after reaching 272,075 mesh elements. As the mesh is further refined, the variation in the calculated results becomes even smaller. Considering both computational accuracy and time cost requirements, 272,075 mesh elements are sufficient to meet the numerical simulation needs. Beyond 272,075 mesh elements, the variation in total pressure at the throat is less than 0.1%, indicating that the influence of mesh resolution on the numerical simulation results is negligible. Therefore, this study uses 270,000 mesh elements for calculation. Meanwhile, the near-wall mesh y^+ range for the 270,000 mesh is 110~300, which satisfies the applicability requirements of the wall function.

3. Results and Discussion

3.1. Verification of the Oka Model

The ground test results are as follows:

Figure 3 illustrates the temporal variation in the chamber pressure during the ground test motor firing. The average operating pressure in the combustion chamber was measured at 13.76 MPa.

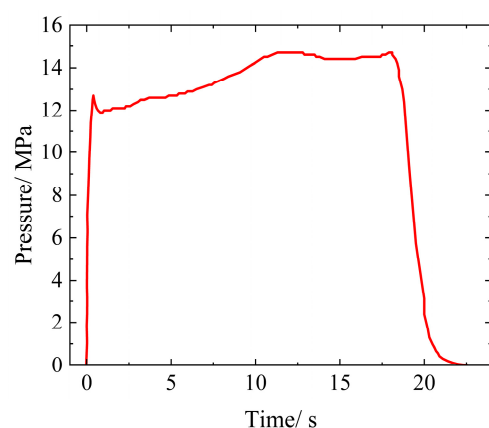


Figure 3. Pressure-Time Curve Inside the Combustion Chamber.

The left panel of Figure 4 shows the macroscopic morphology of the ablated specimen, while the right panel presents the line laser scan profile of the section along the blue line indicated on the left. The line laser instrument used was an LJ-S8000 laser profiler (KEYENCE Corporation, Osaka, Japan). Over the 21.4 s operating period, a comparison of the line laser scan profiles obtained before and after ablation shows that the ablation depth on the upper wall of the front section of the bend was 3.7615 mm, whereas that on the lower wall of the transition section was 0.3752 mm. Notably, flow-induced erosion was observed at the entrance of the upper wall of the transition duct. Under these test conditions, the linear ablation rate of the upper wall in the front section of the elbow was calculated to be 0.1758 mm/s. The experimental measurement uncertainty mainly stems from three aspects: the depth accuracy of the line laser scan is ± 0.02 mm, the measurement accuracy of the ablation time is ± 0.1 s, and the initial surface flatness deviation of the specimen is ± 0.02 mm.

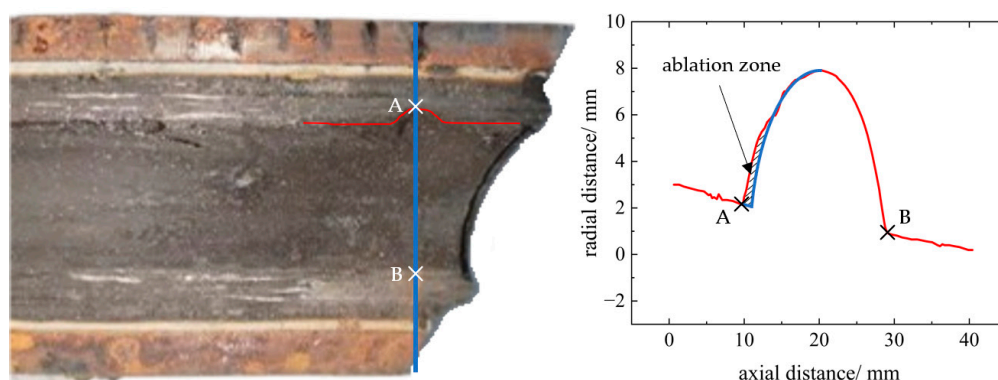


Figure 4. Macroscopic Morphology After Ablation and Line Laser Scanning Results of the Ablated Specimen.

The simulation was conducted under the same operating conditions and gas parameters as those in the ground test.

The computational cases are listed in Table 5.

Table 5. Computational Case Parameters Table.

No.	Chamber Pressure	Gas Generator Exit Throat Diameter	Ablation-Resistant Layer Material for Strut Flow Channel
Case 1	13.80 MPa	6.38 mm	Fine-weave pierced C/C-SiC composite

The key performance parameters of the propellant gas used in this study are shown in Table 6:

Table 6. Main components of the fuel-rich gas.

Type	Component	Mass Fraction	Type	Component	Mass Fraction
Condensed-phase	C	0.100	Gaseous Phase	CO	0.116
	B ₂ O ₃	0.100		HCl	0.015
	BN	0.070		H ₂	0.040
	B ₄ C	0.336		Others	0.149
	Al ₂ O ₃	0.074		Total	0.320
	Total	0.680			

To validate the accuracy of the computational model employed in this study, numerical simulations were conducted using physical models and parameters identical to those of ground tests prior to performing the two-phase flow field calculations.

Figure 5a shows the pressure contour at the XY-axis symmetric plane of the flow field under the baseline condition, where the chamber pressure is approximately 13.3 MPa. A pressure rise region is observed in the nozzle divergent section and at the elbow inlet, attributed to the reduced cross-sectional area of the flow passage in these regions.

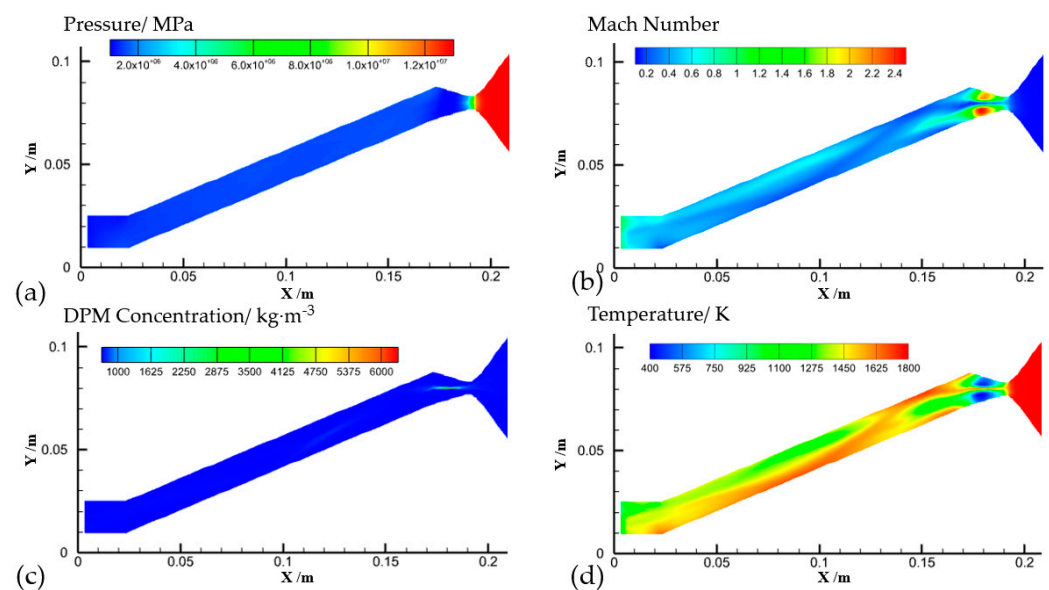


Figure 5. Internal flow field characteristics under the baseline condition: (a) pressure contour, (b) Mach number contour, (c) DPM concentration contour, (d) temperature contour.

Figure 5b shows the Mach number contour for the baseline condition. Due to the high condensed-phase mass loading in this case, the velocity of the condensed-phase lags behind that of the gas phase, resulting in a relatively low Mach number at the throat. Specifically, the Mach number at the throat axis is approximately 0.44, while it reaches about 2.49 near the wall region. Vortex phenomena are also evident in the throat area.

Figure 5c shows the distribution contour of the condensed-phase particles. It can be observed that after converging through the nozzle, the particles impinge upon the upper wall of the elbow, with some rebounding to strike the lower wall of the elbow. Owing to the small throat diameter and strong convergence effect, the maximum particle concentration at the axis of the nozzle divergent section reaches 6552 kg/m³, whereas the concentration at the impact point on the elbow wall is 135.8 kg/m³.

Figure 5d shows the temperature contour. Distinct low-temperature regions are visible on the upper and lower sides of the divergent section. By correlating with Figure 5c, it is evident that the high-temperature regions within the elbow correspond primarily to areas with high concentrations of condensed-phase particles.

The Oka parameters listed in Table 7 were applied to the wall surfaces within the flow model to calculate the ablation behavior, yielding a preliminary wall ablation rate. Here, N_1 and N_2 are the angle correction exponents of the Oka model, while K_1 and K_2 are the velocity and diameter correction exponents. These values are derived from the calibration results for C/C-SiC composite materials [30].

Table 7. Parameters of the Oka Erosion Model.

U_{ref}	D_{ref}	e_{ref}	HV	N_1	N_2	K_1	K_2
110.6 m/s	297 μm	0.00037	15 GPa	0.793	0.762	2.0	2.45

As can be seen from the comparison in Figure 6, the distance between the location of maximum depression in the scanning image and the truncation point is 8 mm; in the simulation results, the distance from the particle scouring location to the elbow turning point is 18 mm. This is because the specimen retrieval design in the experiment resulted in a 10 mm truncation of the elbow. Consequently, the location of maximum ablation in the ground test aligns with the region where condensed-phase particles impact the wall, as shown in the flow field simulation results; both are located at the intersection of the combustion chamber central axis and the elbow wall surface.

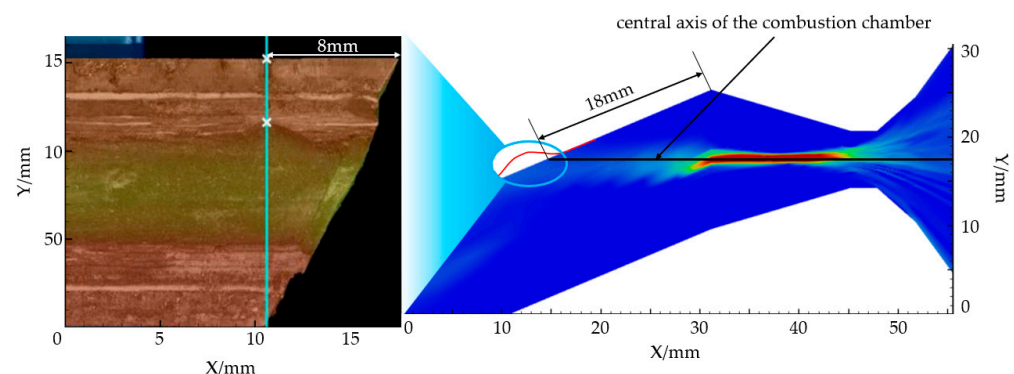


Figure 6. Comparison between Experimental Ablation Zones and Simulated Particle Erosion/Impact Contours.

The computational results indicate that under the baseline operating conditions, the maximum linear ablation rate on the upper wall of the transition section is 0.169 mm/s. In the erosion zone, the condensed-phase particle concentration is 135 kg/m³, and the statistically averaged velocity of the condensed-phase particles in this region is approximately 284 m/s.

The error between the Oka model prediction and the experimentally measured value of 0.1758 mm/s for the upper wall of the transition section is about 3.87%, indicating that this set of Oka parameters can be reasonably applied to analyze and predict the ablation rate under other operating conditions.

3.2. Simulation Results and Coupled-Parameter Fitting

To clearly illustrate the influence trends of different variables on particle erosion rate three operating parameters were selected as listed in Table 8, namely the bend erosion angle condensed-phase content and particle diameter. It should be noted that the particle impact velocity is strongly coupled with the nozzle expansion ratio and flow field struc-

ture. Adjusting the expansion ratio to control velocity would introduce interference from geometric variations; thus, velocity is not treated as an independent variable in this study but rather as a dependent parameter influenced by other variables. A dedicated follow-up investigation will systematically examine the isolated effect of impact velocity by fixing the nozzle geometry and varying the inlet total pressure.

Table 8. Different Operating Conditions for Simulation Calculation.

NO.	Variation Parameter	Elbow Erosion Angle (°)	Condensed-Phase Proportion (/)	Particle Diameter (μm)
1	\	22.7	68%	40
2		22.7	68%	30
3		22.7	68%	35
4		22.7	68%	45
5		22.7	68%	50
6	Condensed Phase Proportion	22.7	58%	40
7		22.7	48%	40
8		22.7	38%	40
9		22.7	28%	40
10	Elbow Erosion Angle	18	68%	40
11		20	68%	40
12		25	68%	40
13		27	68%	40

Condition 1 served as the baseline condition, providing a reference for comparative analysis against other conditions and corresponding to the ablation calculation parameters used for validating the Oka model in Section 3. Conditions 2, 3, 4, 5 constituted the parametric study on particle diameter, with diameters uniformly distributed across the 30–50 μm range. Conditions 6, 7, 8, 9 focused on varying the condensed-phase mass fraction. By maintaining a constant gas-phase mass while adjusting the condensed-phase mass, the fraction was incrementally increased from 28% to 68%. Conditions 10, 11, 12, and 13 examined the effect of the bend erosion angle. The angle of particle impingement on the wall was modified by altering the geometric profile of the bend section. Since this study primarily focused on ablation rate variations at relatively small impact angles, excessively large erosion angles were not considered. Consequently, the investigated range was limited to 18–27°, with values increasing incrementally across the conditions.

3.2.1. The Influence of Particle Diameter on the Ablation Rate

Five particle diameters 30 μm, 35 μm, 40 μm (standard condition), 45 μm, and 50 μm were selected for ablation analysis. As the particle diameter increases, both the mass and inertia of the particles rise significantly. In high-speed gas flows, due to the velocity lag effect, larger particles are more difficult to accelerate to the same velocity as the gas flow compared to smaller ones. Consequently, as shown in Table 9, larger-diameter particles exhibit lower impact velocities at the wall surface than smaller-diameter particles.

Table 9. Particle Velocity with Different Diameters at the Impact Site.

Particle Diameter	30 μm	35 μm	40 μm	45 μm	50 μm
Particle velocity at the Impact Site	325 m/s	300 m/s	284 m/s	269 m/s	241 m/s

As shown in Table 10, the comparison reveals that the larger the particle diameter, the lower the density of particles in the gas flow. This is because larger-diameter particles

possess greater mass, making it more difficult for them to follow the flow streamlines when the gas velocity changes. Consequently, larger particles may deviate from the mainstream due to inertia, impacting the wall or depositing, thereby reducing their concentration in the gas flow. In contrast, smaller-diameter particles have lower inertia, allowing them to move more easily with the gas flow and maintain a higher concentration.

Table 10. Particle Concentrations with Different Diameters at the Impact Site.

Particle Diameter	30 μm	35 μm	40 μm	45 μm	50 μm
Particle Concentration at the Impact Site	220 kg/m^3	150 kg/m^3	135 kg/m^3	105 kg/m^3	85 kg/m^3

As shown in Equation (6), according to the kinetic energy formula of particles:

$$E_k = \frac{1}{2}mv^2 \quad (6)$$

The kinetic energy carried by each particle at the impact point can be calculated as shown in Table 11.

Table 11. Energy Carried by Particles of Different Diameters.

Particle Diameter	30 μm	35 μm	40 μm	45 μm	50 μm
Kinetic energy	1.9×10^{-9} kJ	2.5×10^{-9} kJ	3.4×10^{-9} kJ	4.4×10^{-9} kJ	4.8×10^{-9} kJ

Although large-diameter particles exhibit a relatively low concentration, their higher impact velocities result in greater total kinetic energy upon wall collision. Therefore, the energy carried by larger-diameter particles impacting the wall is still greater. Moreover, the airflow near the wall changes direction, so the larger the particle size, the greater its mass and inertia. This makes it more likely for larger particles to directly impact the material surface in high-speed airflow, leading to mechanical erosion. Research by Chen et al. [36] indicates that 3D C/SiC composites suffer more severe mechanical–chemical erosion from larger particles in high-temperature gas environments, manifested as delamination and peeling of fibers and the matrix. Larger particles carry more energy, resulting in more severe erosion of the wall. Smaller particles, due to their faster diffusion rate, may be carried away by the airflow without contacting the surface, leading to a lower ablation rate.

Finally, the simulation results are shown in Figure 7. The maximum ablation rate for 30 μm particles is 0.101 mm/s, for 35 μm particles is 0.141 mm/s, for the baseline condition of 40 μm particles is 0.169 mm/s, for 45 μm particles is 0.206 mm/s, and for 50 μm particles is 0.232 mm/s. As illustrated in the line chart of particle diameter versus ablation rate, the linear ablation rate on the wall increases with larger particle diameters.

As shown in Figure 8, regardless of the particle size during erosion, the location of the maximum ablation rate remains largely unchanged. The particle diameter does not significantly alter the primary flow path of the gas-particle two-phase flow, with the most severe erosion consistently occurring at the intersection of the nozzle outlet axis and the curved wall surface. Furthermore, as the particle diameter increases, the wall ablation rate gradually rises. Starting from an axial position of 0.02 m, the ablation rate begins to increase from zero. This phenomenon occurs because the particle stream impacts the curved wall at a certain angle, rebounds to strike the lower wall, and then rebounds again to hit the upper wall. After multiple rebounds, the angle of the second impact on the upper wall

becomes too shallow, and the kinetic energy is significantly dissipated. As a result, the ablation-affected area is larger, but the ablation rate is relatively low.

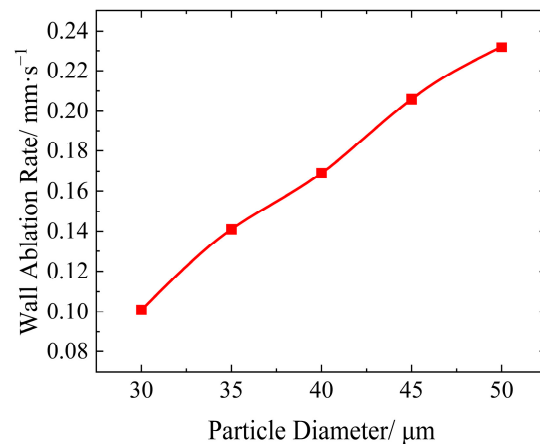


Figure 7. Line Chart of Particle Diameter Versus Wall Ablation Rate.

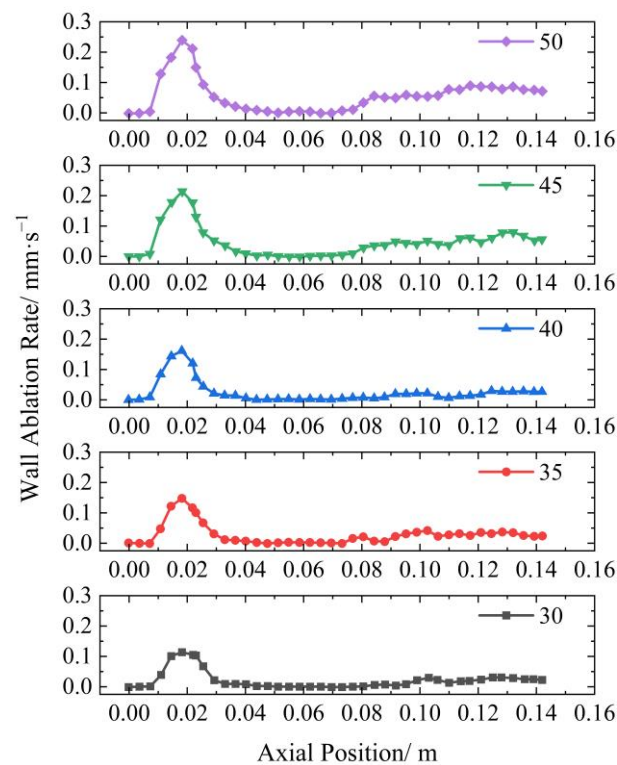


Figure 8. Line Chart of Axial Position Versus Wall Ablation Rate for Different Particle Diameters.

3.2.2. The Influence of Particle Condensed-Phase Fraction on the Ablation Rate

To analyze the effect of particle mass flow rate, gas flow rate must remain constant while only the particulate phase is varied. Based on this fixed gas flow condition, erosion rates were calculated across five operating cases with condensed-phase mass fractions ranging from 28% to 68% to evaluate its influence.

As shown in Figure 9, as the condensed-phase fraction of particles increases, the velocity of particles impacting the upper wall of the bend decreases: 441 m/s at 28%, 409 m/s at 38%, 374 m/s at 48%, 326 m/s at 58%, and 284 m/s at 68%. This occurs because at higher condensed-phase concentrations, the kinetic energy transferred from the gas phase to the condensed phase is distributed among more particles. Consequently, each

individual particle receives less kinetic energy, resulting in a lower velocity compared to cases with lower particle concentrations.

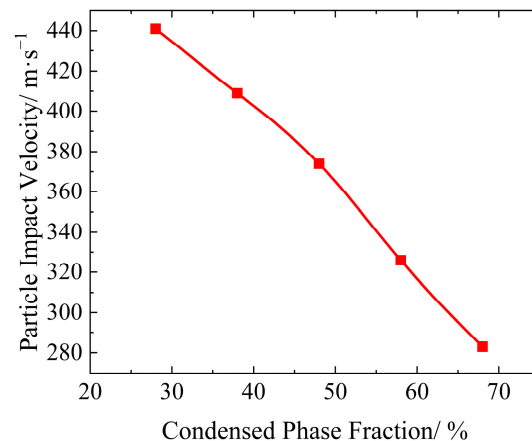


Figure 9. Line Chart of Condensed-Phase Fraction Versus Particle Impact Velocity.

As shown in Figure 10, the ablation rates are as follows: 0.169 mm/s at a condensed-phase concentration of 68%; 0.1507 mm/s at 58%; 0.137 mm/s at 48%; 0.117 mm/s at 38%; and 0.094 mm/s at 28%.

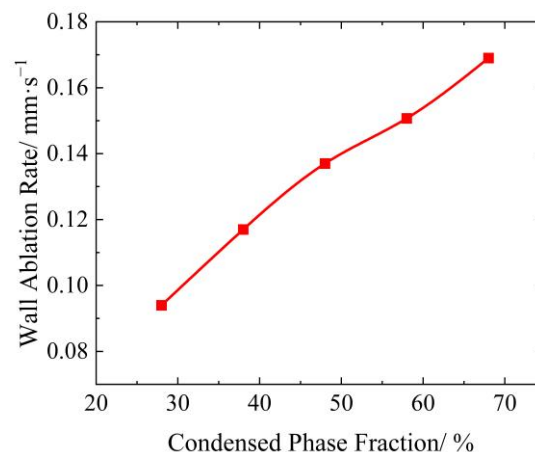


Figure 10. Line Chart of Condensed-Phase Fraction Versus Wall Ablation Rate.

Figure 11 presents a line chart illustrating the relationship between axial position and wall ablation rate under different condensed-phase concentrations. As shown in the figure, for the ablation caused by two-phase flow with high condensed-phase particles, the trend of ablation rate variation with position is similar across different condensed-phase concentrations, and the location of the maximum linear ablation rate in the simulation remains the same. In the five experimental conditions, the mass of the particles remains unchanged, but the impact velocity of the particles increases as the condensed-phase concentration decreases. For individual particles, lower condensed-phase concentration results in higher kinetic energy. However, due to changes in particle inlet flow rate, a higher condensed-phase fraction means more particles, leading to a greater number of particles impacting the wall. Consequently, the total energy carried by the particles increases, resulting in a higher ablation rate.

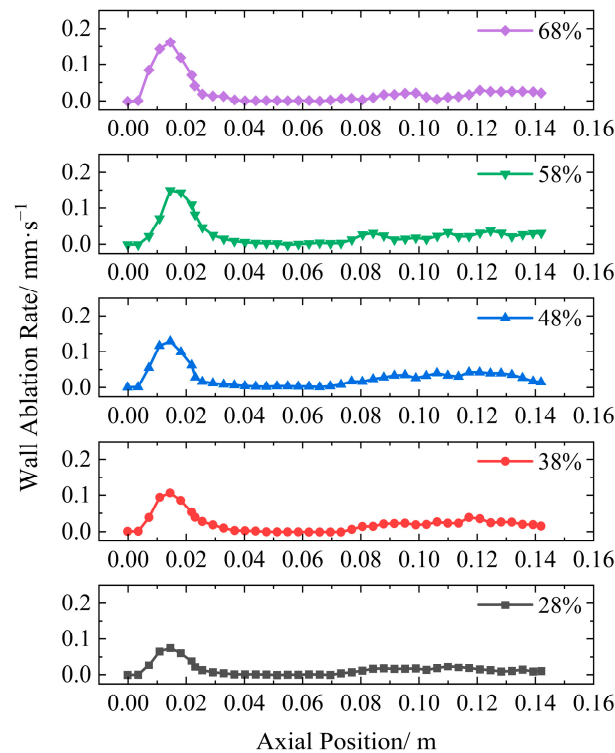


Figure 11. Line Chart of Axial Position Versus Wall Ablation Rate under Different Condensed-Phase Concentrations.

3.2.3. The Influence of Particle Impact Angle on the Ablation Rate

All experiments in this study were conducted under low-angle erosion conditions, with impact angles varying between 0° and 30°. As the impact angle changes, the ablation rate on the material surface exhibits a distinct nonlinear trend.

With decreasing erosion angle, the contact area between particles and the wall expands and shear forces intensify; however, the attenuation of normal forces dominates, leading to a progressive decline in the overall ablation rate. This explains the observed trend of lower ablation rates at smaller impact angles.

As shown in Figure 12, the ablation rates are as follows: 0.1208 mm/s at 18°, 0.139 mm/s at 20°, 0.169 mm/s at 22.7°, 0.223 mm/s at 25°, and 0.31 mm/s at 27°. It can be observed that as the angle increases, the ablation rate also rises, and the rate of increase in ablation rate accelerates with larger angles.

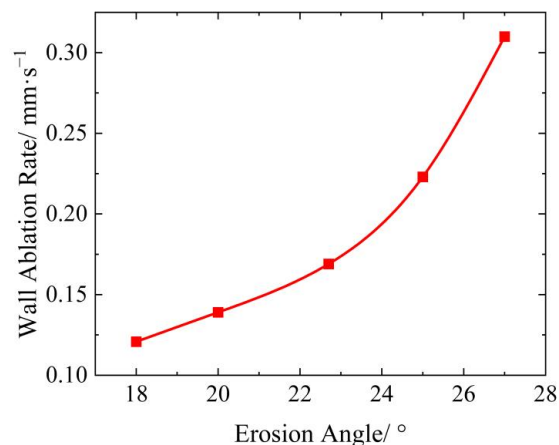


Figure 12. Line Chart of Erosion Angle Versus Wall Ablation Rate.

As shown in Figure 13, by comparing the line charts of different erosion angles, it can be clearly observed that a larger erosion angle corresponds to a higher ablation rate. Additionally, the comparison of line charts reveals that the location of the maximum linear ablation rate obtained through simulation, by varying the bending angle of the elbow, shifts backward as the angle increases. This occurs because changes in the elbow angle cause the intersection point of the nozzle axis and the elbow to move further away from the inlet of the elbow, thereby shifting the particle erosion location backward. Moreover, as the angle changes, the ablation rate in the latter part of the elbow caused by particle beam rebound also varies. This is due to the alteration in the erosion angle of the particle beam rebounding back to the upper wall of the elbow as the angle changes. Consequently, as the elbow angle increases, the rebound ablation rate on the upper wall gradually rises.

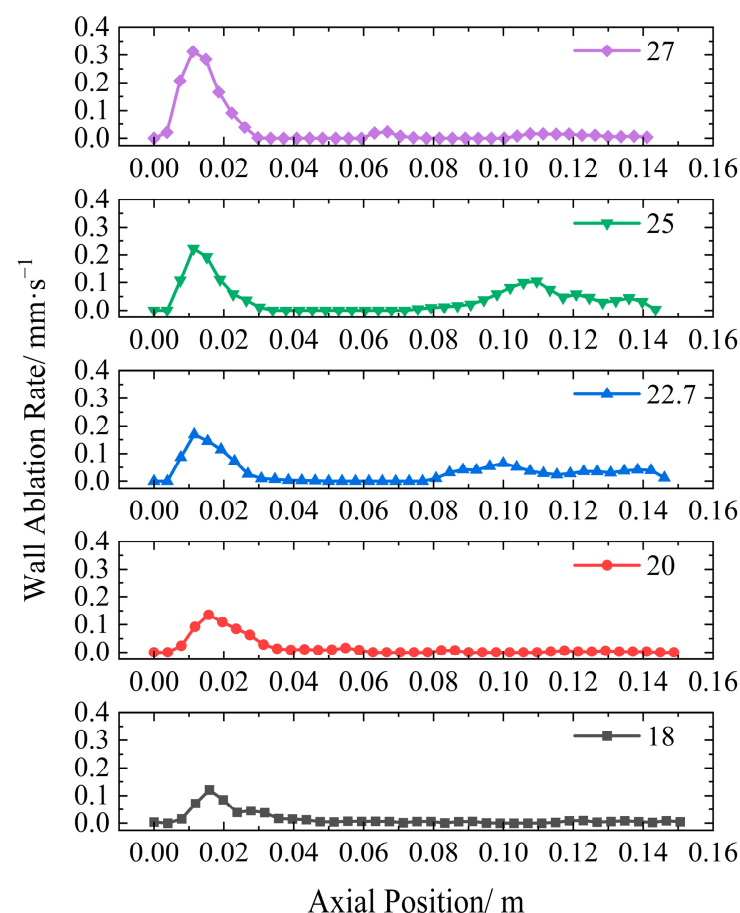


Figure 13. Line Chart of Axial Position Versus Wall Ablation Rate under Different Erosion Angles.

3.2.4. Empirical Formula Regression and Sensitivity Analysis of Wall Linear Ablation Rate Model

Due to the prohibitively high costs associated with ground testing, it is not feasible to conduct ground simulation experiments to measure the wall ablation rate of solid rocket motors under every new operating condition. Therefore, deriving an empirical formula for wall ablation rates to perform preliminary calculations for each new scenario is an effective method to reduce measurement costs.

This section applies multiple linear regression to analyze the simulation data calculated based on the Oka model. Table 12 presents the simulation results.

Table 12. Ablation rate derived from simulation calculations.

NO.	Variation Parameter	Elbow Erosion Angle (°)	Condensed-Phase Proportion (/)	Particle Diameter (μm)	Wall Erosion Rate (mm/s)
1	Particle Diameter	22.7	68%	40	1.69×10^{-1}
2		22.7	68%	30	1.01×10^{-1}
3		22.7	68%	35	1.41×10^{-1}
4		22.7	68%	45	2.06×10^{-1}
5		22.7	68%	50	2.32×10^{-1}
6		22.7	58%	40	1.51×10^{-1}
7	Condensed Phase Proportion	22.7	48%	40	1.37×10^{-1}
8		22.7	38%	40	1.17×10^{-1}
9		22.7	28%	40	9.40×10^{-2}
10	Elbow Erosion Angle	18	68%	40	1.21×10^{-1}
11		20	68%	40	1.39×10^{-1}
12		25	68%	40	2.23×10^{-1}
13		27	68%	40	3.10×10^{-1}

In the study by Wang et al. [37], an empirical formula incorporating angle and particle concentration can be derived. Additionally, Hong's research [38] provides an empirical expression for particle size. Finally, considering the exponential relationship between condensed-phase fraction and particle concentration, the following formula is obtained:

$$e_r = k \sin^a(\alpha) n^b d^{3c} \quad (7)$$

In Equation (7), the impact angle α is characterized by a sine function, consistent with the shear-dominated mechanism under low-angle erosion. The condensed-phase mass fraction n adopts a first-order term, reflecting the linear contribution of particle quantity to the total erosion energy. The particle diameter d uses a cubic term, aligning with the cubic relationship between individual particle mass and diameter and satisfying the physical meaning of the mass component in kinetic energy expressions. Four exponents a , b , and c are then introduced for multiple regression to derive the following fitted equation.

$$e_r = k \sin^a(\alpha) n^b d^{3c} \quad (8)$$

$k = 0.004842, a = 2.29807, b = 0.72928, c = 0.54884$

The R^2 of the obtained empirical formula is 0.94835, and the adjusted R^2 is 0.93113.

As shown in Table 13, the maximum relative error between the fitted calculation formula and the simulated wall ablation rate is 16.6%, which can be used for preliminary engineering estimation of ablation rates. The main reason for the relatively high fitting error under the 27° condition is that the angle correction term $g(\alpha)$ in the Oka model increases non-linearly when $\alpha > 25^\circ$. Since this study only uses five angular sample points, the regression model has limited fitting capability for the high-angle range. Future research will add sample points in the 25° to 30° range to optimize the angle correction term.

Table 13. Relative Error of Fitted Formula.

NO.		Wall Erosion Rate(mm/s)	Empirically Calculated Ablation Rate(mm/s)	Relative Error
1	\	1.69×10^{-1}	1.78×10^{-1}	5.3%
2		1.01×10^{-1}	1.11×10^{-1}	9.7%
3		1.41×10^{-1}	1.43×10^{-1}	1.3%
4	Particle Diameter	2.06×10^{-1}	2.16×10^{-1}	4.9%
5		2.32×10^{-1}	2.57×10^{-1}	10.8%
6	Condensed Phase Proportion	1.51×10^{-1}	1.58×10^{-1}	5.2%
7		1.37×10^{-1}	1.38×10^{-1}	0.8%
8		1.17×10^{-1}	1.16×10^{-1}	0.5%
9		9.40×10^{-2}	9.32×10^{-2}	0.9%
10	Elbow Erosion Angle	1.21×10^{-1}	1.07×10^{-1}	11.6%
11		1.39×10^{-1}	1.35×10^{-1}	3.0%
12		2.23×10^{-1}	2.19×10^{-1}	1.7%
13		3.10×10^{-1}	2.59×10^{-1}	16.6%

3.3. Sobol Global Sensitivity Analysis

To quantify the contribution of input parameters $X = \{x_1, x_2, \dots, x_k\}$ to the uncertainty of model output $Y = f(X)$, this study employs the variance-based Sobol' global sensitivity analysis method. This approach not only identifies the independent effect of individual parameters but also reveals nonlinear interactions among parameters.

This study employs the Saltelli sampling method. The sampling range of the input parameters is consistent with the simulation conditions (particle diameter: 30–50 μm , condensed-phase mass fraction: 28–68%, impact angle: 18–27°). The parameters follow a uniform distribution, with a total sample size of 32,768 sets. Furthermore, the Bootstrap method is used to calculate the 95% confidence interval of the sensitivity indices.

Assuming the model $f(X)$ is square-integrable and satisfies $\int f(X)dX = 0$, according to the Hoeffding decomposition theorem, the total variance $V(Y)$ can be decomposed into a sum of partial variances of various orders:

$$V(Y) = \sum_i V_i + \sum_{i < j} V_{ij} + \sum_{i < j < k} V_{ijk} + \dots + V_{12\dots k} \quad (9)$$

where V_i is the first-order partial variance of parameter x_i ; V_{ij} represents the second-order interaction partial variance between parameters x_i and x_j ; and so on. Based on this decomposition, the Sobol' indices are defined as the proportions of each order of partial variance relative to the total variance.

This study primarily focuses on two types of sensitivity indices:

- (1) First-order Index, S_i

$$S_i = \frac{V_i}{V(Y)} = \frac{V_{x_i}(E_{\sim x_i}[Y|x_i])}{V(Y)} \quad (10)$$

S_i measures the contribution of x_i when it varies independently, accounting for the main effect of the parameter while explicitly excluding higher-order interactions.

- (2) Total-effect Index, S_{Ti}

$$S_{Ti} = \frac{E_{\sim x_i}(V_{x_i}[Y|x_{\sim i}])}{V(Y)} = 1 - \frac{V_{\sim x_i}E_{x_i}[Y|x_{\sim i}])}{V(Y)} \quad (11)$$

The total-effect index S_{Ti} encompasses all first-order and higher-order interaction effects associated with parameter x_i . If S_{Ti} is significantly larger than S_i , it indicates strong coupling between this parameter and other parameters.

(3) Second-order Index, S_{ij}

$$S_{ij} = \frac{V_{ij}}{V(Y)} \quad (12)$$

S_{ij} is specifically used to quantify the impact of pairwise interactions between parameters x_i and x_j on output uncertainty.

However, as this study aims to identify individual parameters and their overall sensitivity rankings, and second-order and higher-order interaction terms suffer from relatively large estimation variances, the subsequent results and analysis will focus on the more robust first-order sensitivity index S_i and total-effect sensitivity index S_{Ti} . Discussions on interaction effects are primarily conducted via the qualitative characterization of the residual term $S_{Ti} - S_i$, and numerical results of the second-order interaction indices S_{ij} will not be presented individually.

The calculated first-order sensitivity indices S_i and total-effect indices S_{Ti} of the Sobol method, along with their 95% confidence intervals, are summarized in Table 14.

Table 14. Results of Sobol's sensitivity analysis with 95% confidence intervals.

	α	n	d
ST	0.6075 [0.5948, 0.6206]	0.9729 [0.9593, 0.9868]	0.6146 [0.6003, 0.6298]
S1	0.4091 [0.3962, 0.4222]	0.9396 [0.9262, 0.9533]	0.4214 [0.4036, 0.4383]

As presented in Figure 14, regarding the total-effect index (S_T), the parameter n exhibits the highest sensitivity, while parameters d and α show nearly identical values. The high S_T value indicates that n is the primary contributor to the output variance of the model, suggesting that the model output is predominantly governed by n within the investigated parameter space.

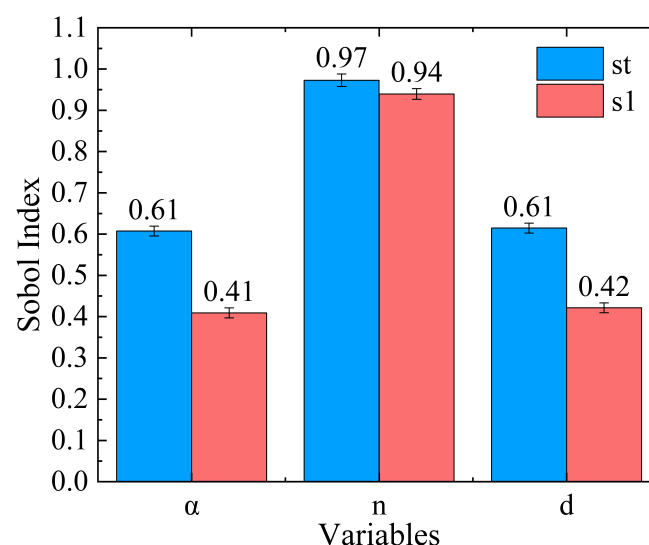


Figure 14. Bar chart of Sobol sensitivity indices with 95% confidence intervals.

Further comparison between the first-order sensitivity index S_1 and the total-effect index S_T reveals that the difference between S_T and S_1 for n is negligibly small, indicating

that its influence on the model output is governed entirely by independent linear superposition. Although the differences for α and d are relatively larger, they remain below 0.2, and the contribution of S_1 to S_T is less than 50%. This suggests only moderate-to-weak interactions among parameters, implying an absence of complex synergistic or antagonistic effects. Consequently, the single-factor control method adopted in this study for selecting operating conditions is justified.

In summary, the sensitivity ranking of the three parameters is: $n > d > \alpha$. The total-effect index for n is relatively high at 0.9729, confirming it as the dominant factor controlling the ablation rate. In contrast, the total-effect indices for d and α are comparatively lower, at 0.6146 and 0.6075, respectively, further corroborating the absence of significant interaction effects on the model output.

4. Conclusions

This study employs the Oka erosion model to investigate the ablation characteristics of C/C-SiC material pipelines exposed to high-temperature, high-velocity combustion gases laden with condensed-phase particles. Three key variables—particle diameter, condensed-phase mass fraction, and impact angle—are systematically analyzed, and the main conclusions are summarized as follows:

- (1) For particle sizes ranging from 30 to 50 μm an increase in diameter reduces the wall impact velocity from 325 m/s to 241 m/s and the concentration from 220 kg/m³ to 85 kg/m³ yet raises the kinetic energy per particle from 1.88×10^{-9} kJ to 4.79×10^{-9} kJ leading to a rise in the linear wall ablation rate from 0.101 mm/s to 0.232 mm/s. With a constant gas-phase velocity, increasing the condensed-phase mass fraction from 28% to 68% lowers the single-particle impact velocity from 441 m/s to 284 m/s but elevates the ablation rate from 0.094 mm/s to 0.169 mm/s due to a higher total number of impacting particles and greater aggregate kinetic energy. Within the small-angle erosion zone of 18–27°, a reduction in impact angle causes the normal impact force to drop far more significantly than the tangential force rises, thereby decreasing the ablation rate from 0.310 mm/s to 0.121 mm/s. The relatively large deviation (16.6%) at 27° is attributed to the limited angular sample points and the nonlinear increase in the Oka angle correction term beyond 25°, which will be addressed by adding sample points in future work.
- (2) Leveraging the established relationship between kinetic energy and ablation rate, an empirical formulation for the linear ablation rate was developed via multiple linear regression. The fitted model exhibits strong explanatory power, yielding a coefficient of determination (R^2) of 0.94835 and an adjusted R^2 of 0.93113. When benchmarked against simulation results, the maximum relative deviation is 16.6%, which is considered acceptable for preliminary engineering assessments. With this predictive model established, a Sobol global sensitivity analysis was subsequently conducted to quantify the contribution of each input parameter to the output variance and their interaction mechanisms. The results identify the condensed-phase mass fraction (n) as the principal factor governing the ablation rate, with a total-effect index (S_T) of 0.9729, whereas the impact angle (α) and particle diameter (d) exert moderate influences, registering S_T values of 0.6075 and 0.6146, respectively. Comparison between the total-effect (S_T) and first-order (S_1) indices reveals negligible interaction effects among the three parameters, indicating that the overall ablation response is predominantly driven by the linear, independent contributions of the individual variables.
- (3) Constrained by the requirement to maintain variable independence in this study, particle impact velocity was not included as an independent variable. In subsequent research, we will implement a single-variable control strategy by fixing the nozzle

geometry while regulating the inlet total pressure to modulate the impact velocity. This approach will systematically elucidate the quantitative influence patterns of velocity on the ablation behavior of C/C-SiC composites and further refine the ablation rate prediction model for extreme operating conditions. Meanwhile, the nozzle exit temperature in this study is approximately 1000 K, at which the B_2O_3 in the condensed-phase particles exists in a liquid state. To simplify the calculation, this study assumes the particles to be in a solid state, and future research will introduce a liquid-solid two-phase particle coupling model for correction.

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Conflicts of Interest: The authors declare no conflicts of interest.

Nomenclature

Latin	Definition	Unit
ρ	Density	kg/m ³
k	Turbulent Kinetic Energy	m ² /s ²
ε	Dissipation Rate	m ² /s ³
t	Time	s
U_i	Mean Velocity	m/s
μ	Dynamic Viscosity	Pa·s
μ_t	Turbulent Viscosity	Pa·s
G_k	Generation of k	kg/(m·s ³)
G_b	Generation by Buoyancy	kg/(m·s ³)
Y_M	Compressibility dissipation term	kg/(m·s ³)
$C_{\varepsilon 1}, C_{\varepsilon 2}, C_{3\varepsilon}$	Empirical constants of the ε -equation	\
$\sigma_k, \sigma_\varepsilon$	Prandtl number	\
S_k, S_ε	User-defined source term	kg/(m·s ³)
a	Acceleration	m/s ²
M	Spatial position	m
V	Velocity	m/s
U, v, w	Velocity component	m/s
er	Erosion	kg/(m ² ·s)
U_{ref}, D_{ref}	Reference particle velocity and diameter	\
e_{ref}	Erosion rate at normal incidence	kg/(m ² ·s)
α	Erosion angle	rad
HV	Vickers hardness	GPa
E_k	Kinetic energy of a single particle	kJ
m	Mass of a single particle	kg
n	Condensed-phase fraction	%
d	Particle diameter	μm

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