

Article

Multi-Objective Cooperative Optimization of Key Structural Parameters of Pressure Swirl Nozzles Under Microgravity

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Abstract

This study presents a surrogate-assisted multi-objective numerical framework to analyze and optimize the coupled effects of geometric parameters on the spray cone angle and discharge coefficient of a pressure swirl nozzle under microgravity conditions. Three-dimensional CFD simulations combined with an orthogonal experimental design were used to construct a structured dataset, on which parameter sensitivity and objective inconsistency were diagnosed using the Analytic Hierarchy Process. Back-propagation neural network surrogate models were then developed and coupled with the NSGA-II algorithm to explore the continuous design space and identify Pareto-optimal solutions. CFD re-evaluations show that, under identical operating conditions, the Pareto-based optimized configuration increases the spray cone angle from 64.71° to 69.14° while improving the discharge coefficient from 0.2736 to 0.3903 relative to the baseline nozzle. The results demonstrate that explicitly accounting for the trade-off between spray spreading and flow capacity enables a more balanced nozzle design than conventional weighted optimization approaches, providing a quantitative reference for microgravity-relevant injector design.

Keywords: pressure swirl nozzle; microgravity; spray cone angle; discharge coefficient; multi-objective optimization

1. Introduction

The construction and operation of the China Space Station (CSS) have substantially expanded the scope of space-based scientific experiments. Within this framework, the Combustion Science Experimental System (CSES) provides a dedicated platform for investigating fundamental combustion phenomena under microgravity conditions [1]. At present, the experimental capabilities of the CSES are mainly focused on gaseous combustion [2–4]. With the progressive development of space combustion research, future experimental configurations are expected to incorporate liquid-fuel-related studies, including single-droplet combustion [5], droplet array combustion [6], and spray combustion [7]. In such experiments, the atomizer constitutes a critical component, as its atomization characteristics directly influence the formation of liquid sprays and, consequently, the stability and controllability of combustion processes. Therefore, understanding and optimizing atomizer performance under microgravity conditions represent an essential prerequisite for the development of liquid combustion experiments in space.



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Pressure swirl nozzles are widely used in propulsion systems, combustion devices, and industrial atomization applications owing to their compact structure, stable operation, and ability to generate relatively wide spray angles [8]. Under normal gravity conditions, extensive research has been conducted to investigate the atomization behavior of pressure swirl nozzles [9]. Previous studies have indicated that key geometric parameters—such as swirl chamber dimensions, contraction and expansion angles, and outlet orifice diameter—play an important role in determining atomization performance indicators, including spray cone angle and discharge coefficient [10,11], as well as droplet size characteristics [12,13]. These relationships have been explored through theoretical analysis, experimental investigation, and numerical simulation, leading to a relatively well-established understanding of geometry–performance coupling for pressure swirl nozzles under terrestrial conditions. In recent years, numerical simulation based on computational fluid dynamics (CFD) has become an important tool for analyzing the internal flow structure and external spray characteristics of pressure swirl nozzles [14–16]. Multiphase flow models, particularly the Volume of Fluid (VOF) method, have enabled detailed descriptions of liquid film formation, breakup, and spray development [11,17]. On this basis, various optimization approaches—such as orthogonal experimental design and response surface methodology [18,19], as well as surrogate modeling using neural networks combined with evolutionary algorithms [20,21]—have been applied to improve atomization performance. Most existing optimization studies focus on single performance objectives or adopt linear weighting strategies to address multiple objectives, and these approaches have been widely applied and shown promising performance for nozzle design under normal gravity conditions [22,23].

However, the majority of the aforementioned studies are conducted under terrestrial gravity, and their conclusions implicitly assume that the coupling relationships between nozzle geometry and atomization performance are insensitive to gravity level. In microgravity environments, the contribution of body forces to the overall flow dynamics is reduced [24], and previous studies indicate that liquid jet breakup and subsequent droplet evolution may differ from those observed under normal-gravity conditions [25–27]. As a result, design principles and optimization strategies derived from normal-gravity conditions cannot be directly assumed to remain valid in microgravity. Although atomization under reduced gravity has attracted increasing attention, systematic numerical investigations addressing how microgravity influences the relationship between nozzle geometry and multiple atomization performance metrics are still limited. More specifically, it remains unclear whether microgravity conditions modify the trade-off mechanism between key atomization performance indicators—such as spray cone angle and discharge coefficient—in pressure swirl nozzles. Conventional optimization strategies generally assume that different performance metrics are governed by similar dominant geometric parameters, or that their relative importance can be adequately represented by fixed weighting coefficients. If, under microgravity conditions, the dominant geometric factors influencing different objectives diverge, linear weighting-based optimization approaches may introduce inherent bias and lead to suboptimal design outcomes. From a numerical modeling perspective, this issue highlights the necessity of explicitly examining multi-objective trade-off characteristics under microgravity, rather than directly extending optimization strategies developed for normal-gravity conditions.

In this context, the present study conducts a purely numerical investigation to examine the multi-objective atomization characteristics of a pressure swirl nozzle under microgravity conditions. A multi-objective cooperative optimization framework is established based on CFD simulations. First, the atomization behavior of a simplified pressure swirl nozzle is numerically analyzed under different inlet pressures and gravity levels using the VOF method. An orthogonal experimental design is then employed to quantify the influence

of key geometric parameters on spray cone angle and discharge coefficient, followed by sensitivity and conflict analysis using the analytic hierarchy process (AHP). To extend the discrete numerical results into a continuous design space, back-propagation neural network surrogate models are constructed to represent the nonlinear mapping between geometric parameters and atomization performance. Finally, the Non-dominated Sorting Genetic Algorithm II (NSGA-II) is applied to identify Pareto-optimal solutions and representative compromise designs under a specified microgravity condition. The objective of this study is to clarify the effect of microgravity on multi-objective trade-off behavior and to provide a numerical reference for the parametric design of pressure swirl nozzles intended for future microgravity combustion experiments.

2. Numerical Model and Computational Method

2.1. Nozzle Geometry and Key Structural Parameters

The pressure swirl nozzle investigated in this study consists of four main internal sections: the inlet section, swirl chamber, contraction section, and outlet section, as illustrated in Figure 1. Multiple inlet passages are uniformly distributed around the swirl chamber. Under a prescribed inlet pressure, the liquid enters the nozzle through these inlet passages and is guided to flow circumferentially along the inner wall of the swirl chamber, thereby forming a strong swirling motion. Due to the high angular momentum of the liquid, a low-pressure region develops near the nozzle axis, which leads to the formation of an air core inside the swirl chamber.

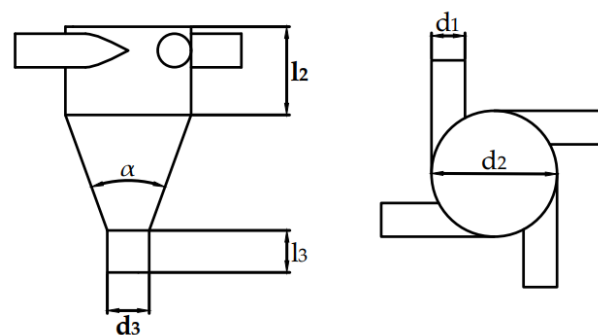


Figure 1. Schematic diagram of the pressure swirl nozzle and its main structural sections.

After passing through the swirl chamber, the liquid is further accelerated in the contraction section and subsequently flows through the outlet section. Under the combined action of the pressure gradient and centrifugal effects, the liquid is driven toward the inner wall of the outlet and forms a conical liquid film with a finite radial velocity component at the nozzle exit. This liquid film provides the initial flow condition for the subsequent atomization process outside the nozzle.

The key geometric parameters of the pressure swirl nozzle include the inlet section diameter d_1 , swirl chamber diameter d_2 , swirl chamber height l_2 , outlet section diameter d_3 , outlet section length l_3 , and contraction half-angle α . The baseline values and variation ranges of these parameters are summarized in Table 1. To ensure consistency in geometric scaling and facilitate comparison among different nozzle configurations, the outlet diameter d_3 is adopted as the reference length for nondimensionalization of the remaining geometric parameters throughout this study.

Table 1. Definition and dimensionless ranges of geometric design variables for the pressure swirl nozzle.

Parameter	Value	Ratio Definition	Ratio Value
d_1/mm	0.48	d_1/d_3	0.8
d_2/mm	1.8	d_2/d_3	3
d_3/mm	0.6	-	-
l_2/mm	1.26	l_2/d_3	2.1
l_3/mm	0.6	l_3/d_3	1
$\alpha/^\circ$	40	-	-

For clarity, geometric angles are denoted by α , while the spray cone angle is denoted by θ in subsequent analyses to avoid ambiguity in notation. All geometric definitions introduced in this section are used consistently in the numerical simulations and optimization procedures described in the following sections.

2.2. Multiphase Flow Model and Simulation Setup

The two-phase flow inside the pressure swirl nozzle and the near-field region downstream of the outlet was simulated using a computational fluid dynamics (CFD) approach [28]. The Volume of Fluid (VOF) method, in conjunction with the continuum surface force (CSF) model, was employed to capture the immiscible gas–liquid interface, and fluid properties were evaluated as volume-fraction-weighted averages of the gas and liquid phases [29]. Turbulence effects were modeled using the RNG k – ϵ turbulence model [30]. Standard wall functions were applied for near-wall treatment, and all solid walls were assumed to be hydraulically smooth with no-slip boundary conditions. This treatment provides an efficient representation of near-wall turbulence in high-Reynolds-number internal flows, which is appropriate for the present pressure swirl nozzle configurations. The assumption of hydraulically smooth, no-slip walls neglects surface roughness effects, thereby focusing the analysis on geometry-induced flow characteristics while excluding roughness-related near-wall losses.

The computational domain included both the internal nozzle passages and the surrounding external region. A pressure-inlet boundary condition was imposed at the nozzle inlet, while the outlet was defined as a pressure outlet with zero gauge pressure. Gravitational acceleration was incorporated through a body-force term in the momentum equations. Simulations were conducted under normal gravity and representative microgravity levels by varying the gravitational acceleration only, while all other boundary conditions and model parameters were kept identical, in order to isolate the effect of gravity on the atomization process.

The governing equations were solved using a transient solution strategy. Pressure–velocity coupling was achieved using the SIMPLE algorithm [31]. The momentum equations were discretized using a second-order upwind scheme, the liquid volume fraction equation was discretized using a geometric reconstruction scheme, and pressure interpolation was performed using the PRESTO! scheme [32]. The time step size was selected to satisfy numerical stability requirements and to adequately resolve the transient evolution of the gas–liquid interface. At the initial time, the entire computational domain was filled with the gas phase, and the liquid phase was introduced through the pressure-inlet boundary. The flow inside the nozzle was treated as turbulent using the RNG k – ϵ turbulence model. Water was used as the working liquid and air as the surrounding gas phase. The density and dynamic viscosity of liquid water were taken as $998 \text{ kg}\cdot\text{m}^{-3}$ and $1.0 \times 10^{-3} \text{ Pa}\cdot\text{s}$, respectively, corresponding to ambient conditions, and both phases were assumed to be incompressible throughout the simulations. Under these operating condi-

tions, the internal flow and atomization process correspond to a turbulence-dominated and inertia-controlled regime, which can be qualitatively characterized using Reynolds- and Weber-number considerations. Each simulation was advanced until a statistically quasi-steady state was reached, after which flow variables were sampled for subsequent analysis. The same numerical setup and initialization procedure were applied to all cases. The reliability of the numerical model was assessed by examining whether the predicted internal flow structures and near-field atomization behaviors are physically reasonable and consistent with those reported in previous studies on pressure swirl nozzles. Key features, including the formation of a stable air core, strong circumferential motion, and the qualitative dependence of spray cone angle and discharge coefficient on operating conditions, support the validity of the adopted modeling approach.

2.3. Computational Mesh and Grid Independence Verification

The computational domain was discretized using the poly-hexcore meshing technique implemented in Fluent Meshing (ANSYS Fluent 2024 R1). The domain consisted of two parts: the internal nozzle flow region and the surrounding external air domain. The internal region included the inlet section, swirl chamber, contraction section, and outlet section, while the external air domain was introduced to resolve the near-field flow downstream of the nozzle exit. The overall mesh distribution and local refinement strategy are shown in Figures 2 and 3. The blue arrows denote the pressure inlet boundary conditions, whereas the pink arrows denote the pressure outlet boundary conditions.

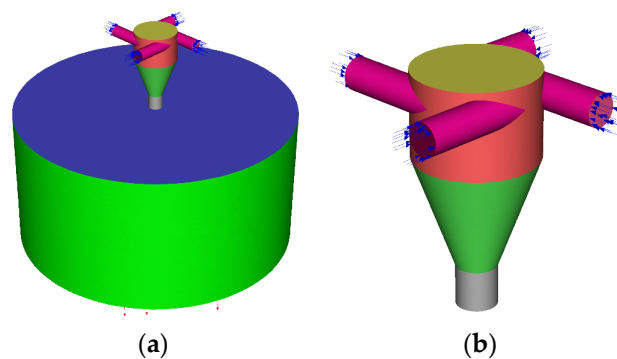


Figure 2. Computational Domain Distribution. (a) Overall computational domain including the pressure swirl nozzle and the external atomization region; (b) computational domain of the pressure swirl nozzle.

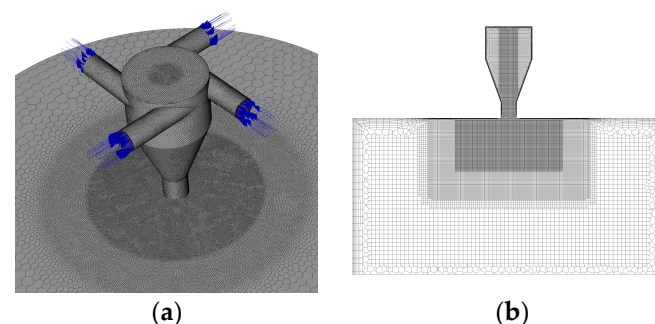


Figure 3. Grid distribution. (a) Surface mesh of the computational domain; (b) mesh distribution on the central cross-section of the computational domain.

Within the poly-hexcore framework, different meshing strategies were applied according to local flow characteristics. Body-of-influence (BOI)-based local mesh refinement was employed in the swirl chamber, contraction section, and outlet region. In the external air domain, the mesh was gradually coarsened with increasing distance from the nozzle exit. A

node-sharing strategy was adopted throughout the entire computational domain to ensure mesh continuity at the interfaces between adjacent regions.

To evaluate grid independence, a series of computational meshes with characteristic cell sizes ranging from 0.0010 mm to 0.0040 mm were generated. For each mesh configuration, simulations were performed using identical physical models, boundary conditions, and numerical settings. The spray cone angle (θ) and discharge coefficient (C_d) were selected as evaluation parameters. The results obtained with the finest mesh, with a characteristic cell size of 0.0010 mm, were taken as the reference solution. The relative deviations of θ and C_d obtained on the other meshes were calculated accordingly. The comparison results are summarized in Table 2.

Table 2. Results of the grid independence verification.

Mesh Size (mm)	Number of Cells ($\times 10^6$)	Spray Cone Angle, θ ($^\circ$)	Relative Deviation of θ (%)	Discharge Coefficient, C_d	Relative Deviation of C_d (%)
0.0010	35.33	61.41	0	0.27795	0
0.0015	11.22	61.72	0.50	0.27562	0.84
0.0020	5.89	62.64	2.00	0.27314	1.73
0.0025	3.23	62.60	1.94	0.27809	0.05
0.003	1.98	60.66	1.22	0.27395	1.44
0.0035	1.32	64.48	5.00	0.27402	1.41
0.0040	1.01	65.18	6.13	0.27402	1.41

As listed in Table 2, increases in the relative deviations of both θ and C_d are observed when the characteristic mesh size exceeds 0.0035 mm. For the mesh with a characteristic size of 0.0030 mm, the relative deviations of the spray cone angle and discharge coefficient are 1.22% and 1.44%, respectively, both within 2%, while the total number of cells is reduced compared with finer mesh configurations. Based on this grid independence verification, the mesh with a characteristic size of 0.0030 mm was selected for all subsequent simulations. Based on this grid independence verification, the numerical solutions were confirmed to be mesh-independent and numerically reliable for subsequent parametric and optimization analyses.

2.4. Definition of Atomization Performance Parameters

The spray cone angle is an important parameter for characterizing the radial expansion of the liquid film and the overall atomization behavior. It is defined as the angle formed by the two outer boundaries of the gas–liquid interface near the nozzle exit, obtained by extending the liquid-film boundaries downstream until they intersect near the nozzle axis.

In this study, the spray cone angle was determined through post-processing of the numerical results using MATLAB-based image processing (MATLAB R2023b). The gas–liquid interface boundary points were extracted from the simulation data, and linear fitting was applied to the two liquid-film boundaries to calculate the spray cone angle. To avoid the influence of the undeveloped flow region immediately downstream of the nozzle exit, the measurement was performed within a selected downstream region away from the outlet. Owing to the unsteady nature of the atomization process, the spray cone angle was time-averaged over the statistically steady stage of the simulation to obtain representative values [33].

The discharge coefficient, C_d , was used to characterize the flow capacity of the nozzle [11]. It is defined as

$$C_d = \frac{\dot{m}_{actual}}{\dot{m}_{theoretical}} = \frac{\dot{m}_{actual}}{A \sqrt{2\rho \Delta p}} \quad (1)$$

where $\dot{m}_{actual}$ is the actual mass flow rate through the nozzle, which is obtained by integrating the mass flux over the nozzle outlet section in the numerical simulations; $\dot{m}_{theoretical}$ is the theoretical mass flow rate; Δp denotes the pressure difference between the nozzle inlet and outlet; A is the outlet cross-sectional area of the nozzle; and ρ represents the liquid density.

3. Multi-Objective Optimization Framework

3.1. Multi-Objective Problem Formulation

The atomization performance of a pressure swirl nozzle is influenced by multiple geometric parameters and is commonly evaluated using more than one performance indicator. In practical nozzle design, enhancement of spray spreading characteristics is often accompanied by variations in flow capacity, leading to an inherent trade-off between competing objectives. In addition, the strongly three-dimensional swirling flow and gas–liquid interaction inside the nozzle result in nonlinear relationships between geometric parameters and atomization performance. Under these conditions, direct multi-objective optimization relying solely on high-fidelity CFD simulations is computationally inefficient.

In the present study, the optimization problem is formulated using six dimensionless geometric parameters that describe the internal structure of the nozzle as design variables. These parameters collectively define the geometry of the swirl chamber, contraction section, and outlet region, and their admissible ranges are constrained by practical design considerations. The spray cone angle (θ) and the discharge coefficient (C_d) are selected as the optimization objectives, representing the spray spreading behavior and the flow capacity of the nozzle, respectively. Accordingly, the optimization task is defined as the simultaneous maximization of the spray cone angle and the discharge coefficient with respect to the selected design variables.

To address this problem in a systematic and computationally efficient manner, a collaborative multi-objective optimization strategy is adopted, as summarized in Figure 4. The strategy integrates orthogonal experimental design, sensitivity and conflict analysis, surrogate modeling, and evolutionary multi-objective optimization, with each component serving a distinct and complementary role within the overall framework.

First, an orthogonal experimental design is employed to generate a structured set of sample points within the discrete design space and to construct a CFD-based dataset. This stage aims to ensure representative sampling of the design variables and to provide a consistent data foundation for subsequent analysis, rather than to directly identify an optimal configuration. Based on the orthogonal experimental results, sensitivity and conflict characteristics of the design variables with respect to the two objectives are examined using the Analytic Hierarchy Process (AHP). The AHP-based analysis provides stage-wise insight into the relative influence of geometric parameters and the potential competition between objectives within the discrete design space, without serving as a final optimization criterion.

On this basis, surrogate models based on back-propagation (BP) neural networks are constructed to establish continuous nonlinear mappings between the design variables and the objective functions. These surrogate models enable efficient evaluation of candidate designs without repeated CFD simulations. The trained surrogate models are subsequently coupled with the non-dominated sorting genetic algorithm II (NSGA-II) to perform multi-objective optimization in the continuous design space. Through this procedure, Pareto-optimal solutions characterizing the trade-off between the spray cone angle and the discharge coefficient can be identified for further examination.

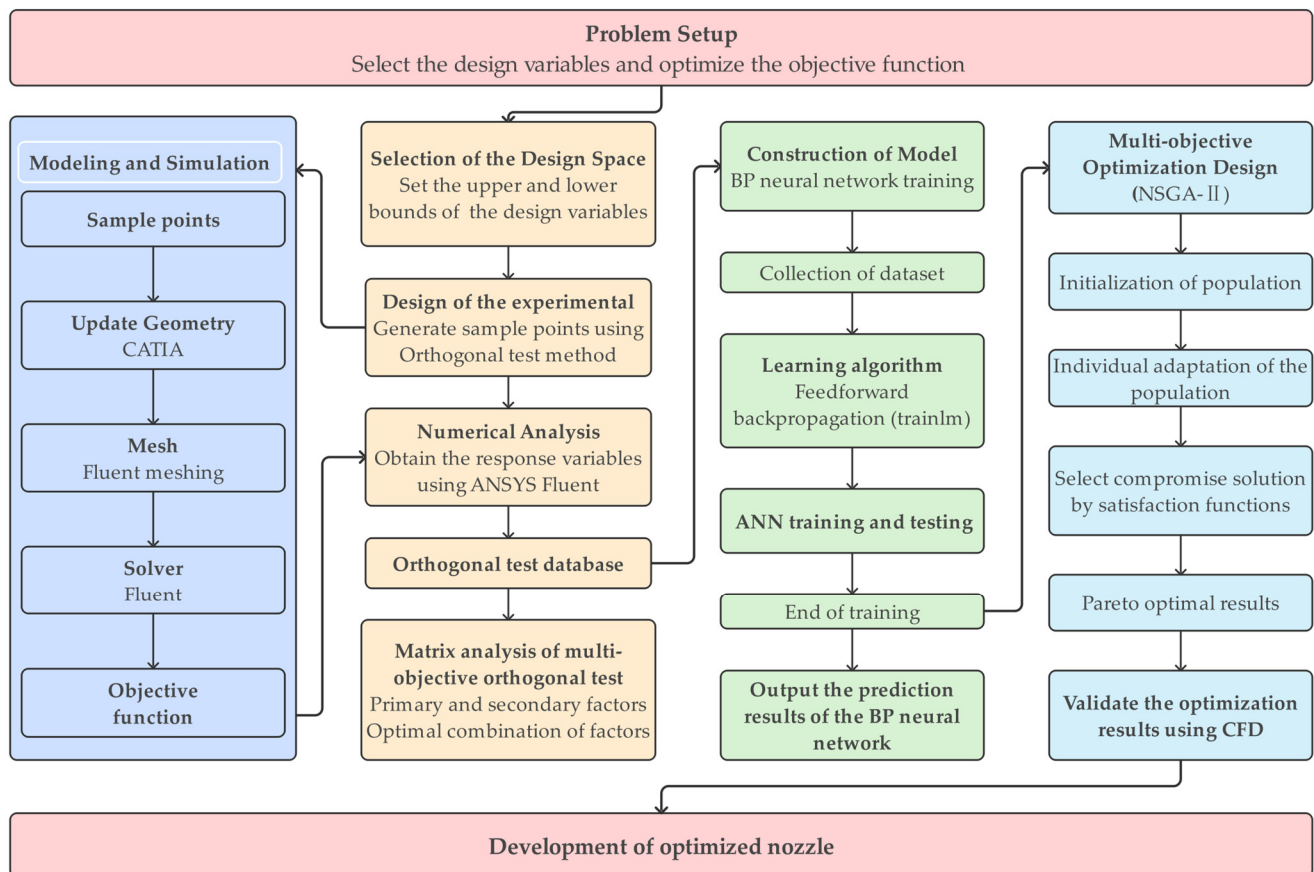


Figure 4. Overall workflow of the collaborative multi-objective optimization framework adopted in this study. Different background colors distinguish the major functional modules.

3.2. Orthogonal Experimental Design and Parameter Selection

Following the problem formulation and overall optimization strategy outlined in Section 3.1, an orthogonal experimental design was employed to discretize the design space and generate a structured dataset for numerical analysis [34]. This stage is intended to provide representative combinations of geometric parameters for subsequent analysis, through a balanced and computationally efficient sampling of the design space, rather than to directly identify an optimal nozzle configuration.

In this study, the spray cone angle (θ) and the discharge coefficient (C_d) were selected as response parameters to characterize the atomization performance of the pressure swirl nozzle [35]. Based on the geometric definitions of the nozzle described in Section 2.1, six dimensionless geometric parameters were chosen as design factors. These parameters collectively describe the internal nozzle geometry, including the outlet diameter, the ratio of outlet length to outlet diameter, the contraction angle, the ratio of swirl chamber diameter to outlet diameter, the ratio of inlet diameter to outlet diameter, and the ratio of swirl chamber length to swirl chamber diameter.

To discretize the design space in a controlled manner, six representative levels were assigned to each design factor. The corresponding factor levels and parameter ranges are summarized in Table 3. An orthogonal array was then constructed based on the selected factors and levels to generate a set of representative nozzle configurations [36].

CFD simulations were conducted for all orthogonal experimental cases using identical physical models, boundary conditions, and numerical settings. The resulting spray cone angle and discharge coefficient were extracted from the simulation results to form the orthogonal experimental database.

Table 3. Factors and levels of the orthogonal experiment.

Level	Factor A	Factor B	Factor C	Factor D	Factor E	Factor F
1	0.6	0.7	10	2.5	0.7	0.5
2	0.8	1.0	20	3	0.8	0.7
3	1.0	1.3	30	3.5	0.9	0.9
4	1.2	1.6	40	4	1	1
5	1.4	1.9	50	4.5	1.1	1.2
6	1.6	2.2	60	5	1.2	1.5

Note: A represents the outlet diameter (d_3); B represents the ratio of outlet length to outlet diameter (l_3/d_3); C represents the contraction angle (α); D represents the ratio of swirl chamber diameter to outlet diameter (d_2/d_3); E represents the ratio of inlet diameter to outlet diameter (d_1/d_3); F represents the ratio of swirl chamber length to swirl chamber diameter (l_2/d_2).

3.3. Sensitivity and Conflict Analysis Based on the Analytic Hierarchy Process

Based on the orthogonal experimental results described in Section 3.2, an Analytic Hierarchy Process (AHP) was employed to analyze the relative influence of geometric parameters on the spray cone angle and the discharge coefficient [37]. It should be emphasized that the purpose of this analysis is not to identify an optimal configuration, but rather to provide a parameter-level diagnostic assessment within the discrete design space defined by the orthogonal experiment. The analysis was conducted using the orthogonal experimental dataset summarized in Table A1 (Appendix A), and the resulting weight distributions for the spray cone angle and the discharge coefficient are reported in Tables A2 and A3 (Appendix A), respectively.

Figure 5 presents a comparison of the AHP-based weight distributions for the two objectives, indicating that the relative importance rankings of the geometric parameters differ between the spray cone angle and the discharge coefficient within the investigated design space. It should be noted that a trade-off between spray spreading and flow capacity is generally expected for pressure swirl nozzles; however, the role of the present AHP-based analysis is not to demonstrate the existence of such a trade-off, but to diagnose parameter-level inconsistency when the two objectives are evaluated independently within a discrete orthogonal design space. Owing to the discrete sampling of the orthogonal experiments and the linear aggregation inherent in the AHP framework, this analysis cannot capture the continuous trade-off relationship between the two objectives, but instead provides methodological motivation for introducing surrogate-based multi-objective optimization in a continuous design space, which is addressed in the subsequent sections.

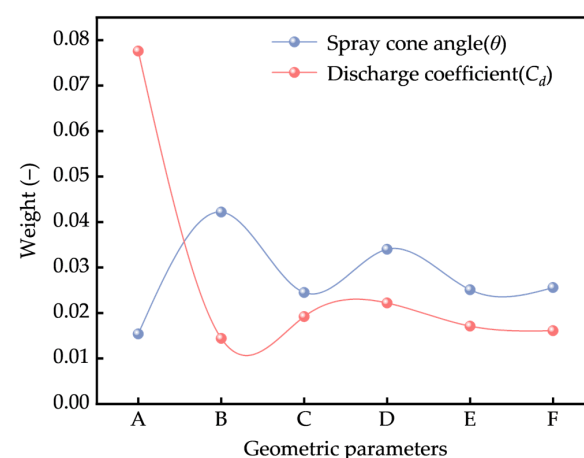


Figure 5. AHP-based weight distributions of geometric parameters for the spray cone angle and the discharge coefficient, used to diagnose parameter-level inconsistency within the discrete orthogonal design space.

3.4. Surrogate Modeling Using Back-Propagation Neural Networks

To enable efficient multi-objective optimization in a continuous design space, back-propagation (BP) neural network surrogate models were constructed to approximate the nonlinear relationships between nozzle geometric parameters and atomization performance indicators based on CFD simulation data [21]. A total of 72 CFD samples were used for surrogate modeling, all generated using orthogonal experimental design. The dataset consists of the 36 samples described in Section 3.2 and an additional 36 samples obtained by reordering the factor-level combinations within the same orthogonal design framework. Each sample comprised six dimensionless geometric parameters as input variables, while the spray cone angle (θ) and the discharge coefficient (C_d) were selected as output responses. All input and output variables were normalized prior to training, and the dataset was randomly divided into training, validation, and testing subsets with proportions of 70%, 15%, and 15%, respectively.

A feedforward BP neural network was adopted as the surrogate modeling framework [38]. The hidden layer employed the tansig activation function, and the output layer used the purelin function, with network training performed using the Levenberg–Marquardt algorithm. Considering the distinct nonlinear response characteristics associated with θ and C_d , two independent BP neural network surrogate models were constructed for the two responses, rather than using a single multi-output network [39]. For the spray cone angle surrogate model, the correlation coefficients for the training, validation, and testing datasets were $R = 0.8984$, 0.8581 , and 0.798 , respectively, with a testing root mean square error (RMSE) of 5.705° . For the discharge coefficient surrogate model, the corresponding correlation coefficients were $R = 0.959$, 0.8473 , and 0.9824 , with a testing RMSE of 0.032 . The regression results, shown in Figure 6, indicate reasonable agreement between the surrogate predictions and the CFD results across different data subsets. On this basis, the constructed surrogate models were employed to replace direct CFD evaluations in the subsequent multi-objective optimization procedure.

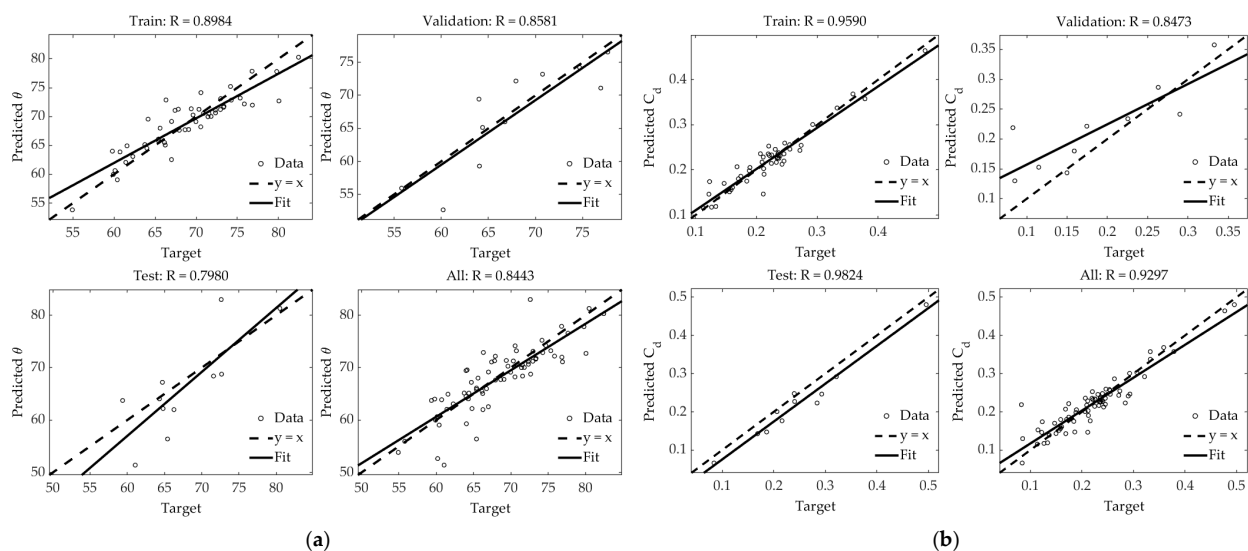


Figure 6. Regression results of BP neural network surrogate models for (a) spray cone angle and (b) discharge coefficient based on CFD data.

3.5. Multi-Objective Optimization Using NSGA-II

Based on the surrogate models established in Section 3.4, a multi-objective optimization framework was formulated to investigate the trade-off relationship between the spray cone angle (θ) and the discharge coefficient (C_d) [34]. The optimization problem was defined as a bi-objective maximization task, in which the six dimensionless geometric parameters

of the pressure swirl nozzle were selected as design variables with variation ranges constrained according to the parameter levels used in the orthogonal experimental design. During the evolutionary search, the objective function values of θ and C_d were evaluated using the previously constructed BP neural network surrogate models, thereby avoiding repeated CFD simulations and enabling efficient exploration of the design space [21].

The optimization was performed using the Non-dominated Sorting Genetic Algorithm II (NSGA-II) with a real-coded genetic representation [40]. The population size was set to 100, and the maximum number of generations was set to 200. A crossover probability of 0.9 and an adaptive mutation strategy were employed to maintain population diversity. To reduce the influence of stochastic effects inherent in evolutionary algorithms, the optimization procedure was executed multiple times under identical algorithmic settings. The resulting non-dominated solutions constitute the Pareto-optimal solution set within the defined design space and provide the basis for subsequent analysis of the trade-off characteristics between the two objectives.

4. Results and Discussion

4.1. Flow and Atomization Characteristics Under Different Pressure and Gravity Levels

The internal pressure and velocity distributions play an essential role in shaping the discharge and spray behavior of pressure swirl nozzles. This section examines the internal flow field and near-field atomization characteristics of the pressure swirl nozzle under different inlet pressures and gravity levels. The objective is to clarify the baseline flow and spray behavior prior to structural optimization and to identify a representative operating condition for subsequent parametric and multi-objective analyses.

At an inlet pressure of 0.3 MPa under normal gravity, the internal flow exhibits a stable swirling structure. Figure 7 shows the pressure distribution on the longitudinal section, where a pronounced low-pressure region develops along the nozzle axis and extends from the swirl chamber toward the outlet. This axial pressure depression is associated with the strong rotational motion of the liquid inside the swirl chamber. Correspondingly, Figure 8 presents the velocity distribution, indicating significant acceleration of the flow in the contraction and outlet sections, with elevated axial velocities concentrated near the nozzle exit.

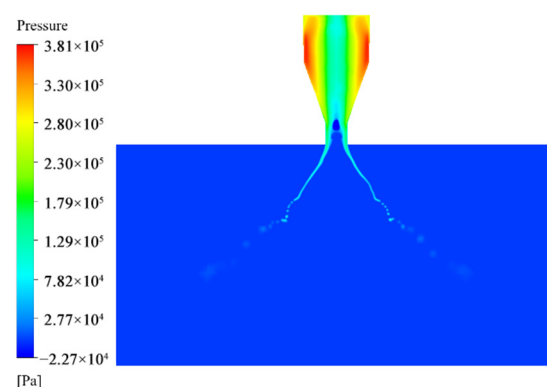


Figure 7. Pressure distribution on the longitudinal section of the flow field.

Further insight into the three-dimensional flow structure is provided by Figures 9–11, which illustrate the velocity vector projection as well as the circumferential and radial velocity components. A pronounced circumferential velocity component is observed within the swirl chamber, confirming the establishment of a stable swirling flow. Meanwhile, the radial velocity component increases noticeably toward the outlet region, indicating enhanced radial momentum of the liquid film. The coexistence of strong circumferential

motion and increasing radial velocity near the nozzle exit provides favorable conditions for liquid film thinning and radial expansion, which is consistent with the observed spray development downstream of the outlet.

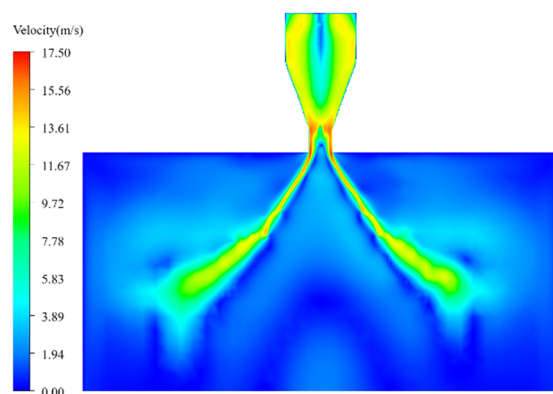


Figure 8. Velocity distribution on the longitudinal section of the flow field.

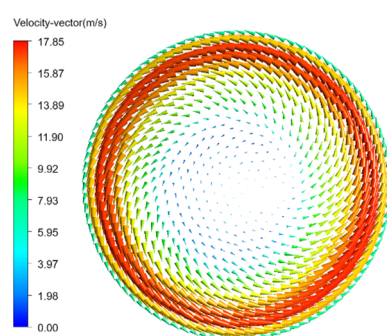


Figure 9. Vertical projection of velocity vectors.

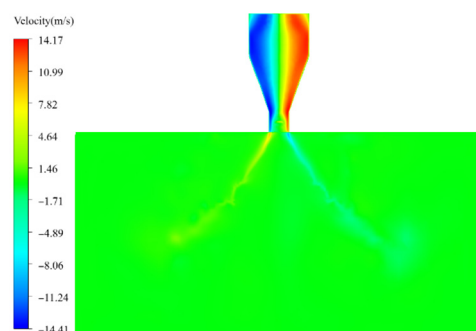


Figure 10. Circumferential velocity distribution on the longitudinal section of the flow field.

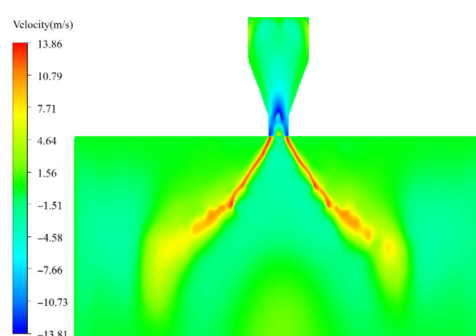


Figure 11. Radial velocity distribution on the longitudinal section of the flow field.

The transient evolution of the gas–liquid interface during the initial atomization process is illustrated in Figure 12 through instantaneous volume-of-fluid contours at several representative time instants. At the early stage of injection, a highly unsteady flow field is observed, characterized by dispersed gas regions above the swirl chamber. As the flow develops, a continuous air core gradually forms along the nozzle axis, while the surrounding liquid film becomes thinner and expands radially. When the flow approaches a quasi-steady state, the air-core structure and liquid film geometry exhibit limited temporal variation, indicating that the internal flow field has stabilized and provides a steady inlet condition for the subsequent atomization process.

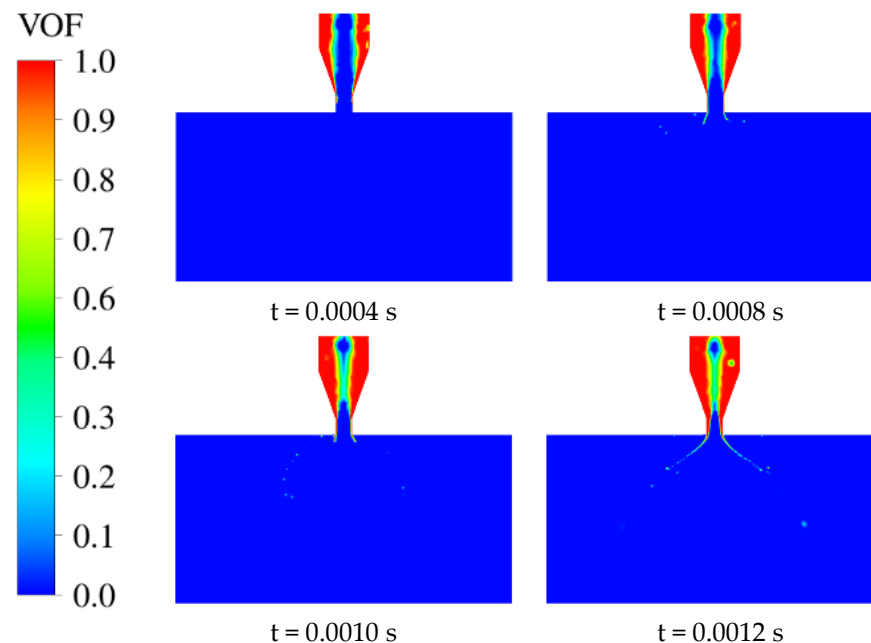


Figure 12. Evolution of the air core at different time instants.

The influence of inlet pressure on atomization performance is examined by varying the inlet pressure from 0.1 to 0.8 MPa under normal gravity. Figure 13 shows the variation of spray cone angle and discharge coefficient with inlet pressure, from which it can be observed that the spray cone angle increases monotonically as the inlet pressure increases. A more pronounced increase occurs in the low-pressure range between 0.1 and 0.3 MPa, whereas the growth rate becomes more gradual at higher pressures. In contrast, the discharge coefficient exhibits a non-monotonic dependence on inlet pressure, increasing initially and reaching a maximum at approximately 0.3 MPa, followed by a slight decrease with further pressure increase. These trends indicate that increasing inlet pressure enhances spray spreading while simultaneously intensifying internal flow resistance and energy dissipation.

The coupled effects of inlet pressure and gravity on atomization performance are further examined by varying the gravity level from 1 g to 10^{-5} g under otherwise identical conditions. Figures 14 and 15 illustrate the variations of spray cone angle and discharge coefficient with gravity level. Figure 14 is intended to serve as a reference case at a fixed inlet pressure, highlighting the isolated influence of gravity on atomization characteristics, whereas Figure 15 extends the analysis to coupled gravity–pressure conditions over a wider operating range. Both parameters show noticeable changes as gravity decreases from 1 g to 10^{-1} g. However, when gravity is further reduced to 10^{-2} g and below, the variations in both parameters become relatively small. This trend is consistently observed across the examined pressure range, indicating that gravity primarily influences the transition

behavior from normal-gravity to reduced-gravity conditions, whereas further reductions in gravity have a limited impact on the steady atomization characteristics. At the lowest injection pressure of 0.1 MPa, the reduced injection momentum enhances the influence of internal flow structure and viscous effects on spray development, leading to a distinct variation pattern compared with higher-pressure cases under microgravity conditions.

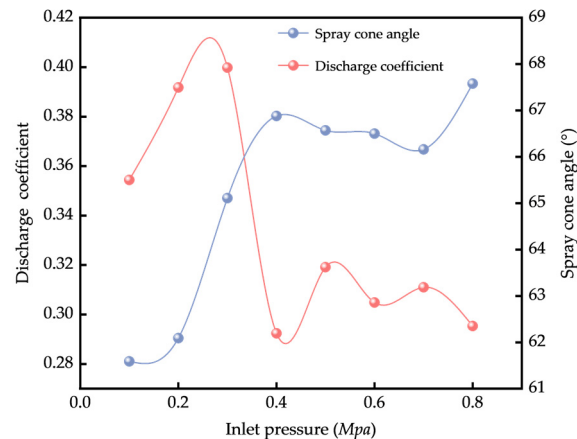


Figure 13. Variations of spray cone angle and discharge coefficient with inlet pressure at 1 g for the baseline structure.

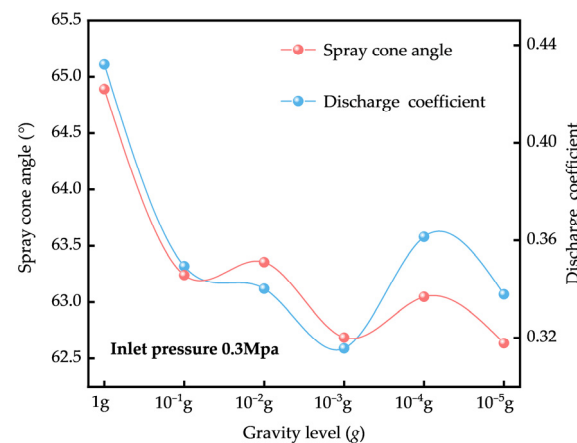


Figure 14. Variations of spray cone angle and discharge coefficient with gravity level at an inlet pressure of 0.3 MPa for the baseline structure.

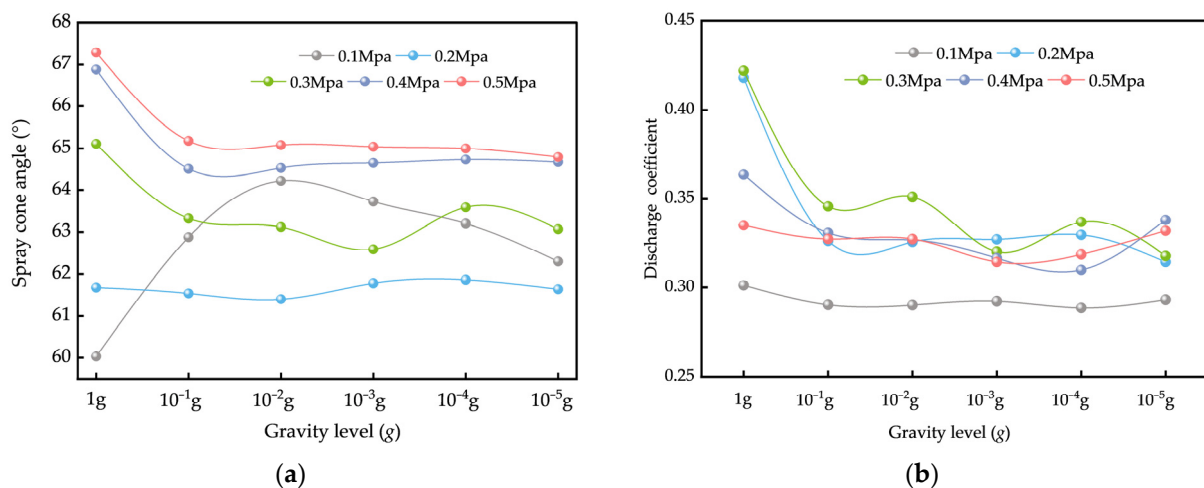


Figure 15. Variations of spray cone angle and discharge coefficient with gravity level under different injection pressures. (a) spray cone angle; (b) discharge coefficient.

Based on the pressure- and gravity-dependent trends of the spray cone angle and discharge coefficient described above, an inlet pressure of 0.3 MPa and a gravity level of 10^{-4} g are selected as the representative operating condition for subsequent analyses. Under this condition, the atomization characteristics are well developed and exhibit reduced sensitivity to further gravity reduction, providing a stable and consistent basis for investigating the effects of nozzle structural parameters and for conducting multi-objective optimization in the following sections. The flow features described above provide the physical background for interpreting the nozzle performance parameters discussed in the subsequent sections.

4.2. Orthogonal Experimental Results and Parameter Influence Trends

Based on the orthogonal experimental dataset combined with the Analytic Hierarchy Process described in the previous section, the single-indicator weight distributions corresponding to the spray cone angle and the discharge coefficient were obtained and are summarized in Tables A2 and A3 (Appendix A), respectively. On this basis, the combined weight distributions of different structural factors at various parameter levels were further derived, and the results are presented in Table 4.

Table 4. Weight values of different levels for each structural factor obtained from orthogonal experiment and AHP analysis.

Factor A	Weight	Factor B	Weight	Factor C	Weight	Factor D	Weight	Factor E	Weight	Factor F	Weight
ω_{A1}	0.0193	ω_{B1}	0.0278	ω_{C1}	0.0202	ω_{D1}	0.0298	ω_{E1}	0.0209	ω_{F1}	0.0202
ω_{A2}	0.0304	ω_{B2}	0.0293	ω_{C2}	0.0235	ω_{D2}	0.0289	ω_{E2}	0.0204	ω_{F2}	0.0209
ω_{A3}	0.0415	ω_{B3}	0.0295	ω_{C3}	0.0231	ω_{D3}	0.0281	ω_{E3}	0.0222	ω_{F3}	0.0201
ω_{A4}	0.0480	ω_{B4}	0.0274	ω_{C4}	0.0239	ω_{D4}	0.0273	ω_{E4}	0.0221	ω_{F4}	0.0217
ω_{A5}	0.0605	ω_{B5}	0.0281	ω_{C5}	0.0225	ω_{D5}	0.0286	ω_{E5}	0.0204	ω_{F5}	0.0218
ω_{A6}	0.0758	ω_{B6}	0.0276	ω_{C6}	0.0219	ω_{D6}	0.0256	ω_{E6}	0.0204	ω_{F6}	0.0203
$\bar{\omega}_A$	0.0459	$\bar{\omega}_B$	0.0283	$\bar{\omega}_C$	0.0225	$\bar{\omega}_D$	0.0281	$\bar{\omega}_E$	0.0211	$\bar{\omega}_F$	0.0208

Table 4 shows that the structural factors contribute unequally to the combined atomization performance when the spray cone angle and the discharge coefficient are considered simultaneously. Among the investigated parameters, factor A exhibits the largest combined weight, followed by factors B and D, whereas factors E and F are associated with comparatively smaller weights. This distribution indicates a clear differentiation in the relative importance of geometric parameters within the discrete design space defined by the orthogonal experiment.

An examination of the combined weight variations across different parameter levels further reveals distinct response characteristics among the structural factors. The combined weight associated with factor A increases monotonically with increasing level number within the investigated range, indicating a progressively strengthening contribution to the combined performance metric. In contrast, the combined weights of factors B, C, D, E, and F display only limited variations across levels and do not exhibit a pronounced monotonic trend. Specifically, factors B and C show weak non-monotonic fluctuations, factor D presents a slight decreasing tendency, and factors E and F remain nearly constant. These results suggest that, within the prescribed parameter ranges, the latter factors exert relatively uniform and moderate influences on the combined atomization performance.

Further insight is obtained by comparing the single-indicator weight distributions of the spray cone angle and the discharge coefficient. As shown in Tables A2 and A3 (Appendix A), the sensitivity of the spray cone angle to the geometric parameters differs substantially from that of the discharge coefficient. Parameters that play a dominant role in promoting spray spreading are not necessarily those governing flow capacity, resulting in distinct importance rankings for the two performance indicators. This divergence

reflects the different geometric mechanisms underlying spray expansion and internal flow resistance within the nozzle.

Under these conditions, the combined weight obtained by aggregating the single-indicator weights represents a linear compromise between the two objectives within the discrete orthogonal design space. While this approach is effective for identifying parameter influence trends and performing preliminary screening, it does not fully capture the continuous trade-off relationship between the spray cone angle and the discharge coefficient. This limitation highlights the need for a subsequent multi-objective optimization analysis in a continuous design space, which is addressed in the following section.

4.3. Pareto Front Characteristics of Multi-Objective Optimization

As indicated by the orthogonal experimental and weighting analyses in Section 4.2, the spray cone angle and the discharge coefficient are governed by different dominant geometric parameters, making it difficult to characterize their coupled behavior using a single weighted objective. To clarify the inherent trade-off between spray spreading performance and flow capacity, a multi-objective optimization was conducted in a continuous design space using BP neural network surrogate models, and the resulting solution distribution was analyzed.

The objective-space distribution of the optimization results is shown in Figure 16. The solutions span a wide range of spray cone angles, from approximately 64° to over 80° , while the corresponding discharge coefficient decreases from about 0.44 to nearly 0.20. Such an inverse relationship between spray cone angle and discharge coefficient has been widely reported in previous experimental and numerical studies on pressure swirl atomizers. An overall inverse relationship between the two objectives is clearly observed, reflecting the intrinsic conflict between enhanced spray spreading and the preservation of flow capacity in pressure swirl nozzles. At relatively small spray cone angles, the discharge coefficient remains high and decreases gradually as the spray cone angle increases. As the spray cone angle increases beyond approximately 70° , the decline in discharge coefficient becomes increasingly pronounced, indicating a progressively strengthening trade-off between spray spreading and flow capacity. In particular, when the spray cone angle exceeds roughly $75\text{--}77^\circ$, the discharge coefficient drops rapidly from about 0.32 to below 0.25, suggesting that further enhancement of spray spreading is achieved at the expense of disproportionately large flow losses.

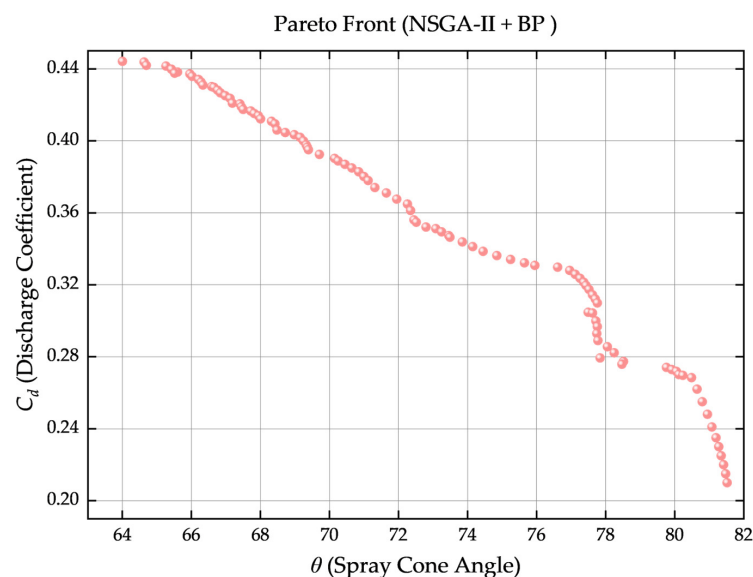


Figure 16. Pareto front of spray cone angle and discharge coefficient.

It should be noted that the solutions are not uniformly distributed along the Pareto front. This non-uniformity primarily arises from the strong nonlinear coupling between geometric parameters and atomization performance metrics, whereby relatively small changes in certain parameters can induce substantial degradation in flow capacity, causing many candidate designs to become dominated. From an engineering perspective, the Pareto front thus provides a rational basis for identifying compromise solutions that balance spray spreading and flow capacity, motivating the selection of a representative optimized configuration for further verification in the following section. This compromise solution reflects the inherent trade-off between spray spreading and flow capacity commonly encountered in pressure swirl nozzle design.

4.4. Comparative Performance of Different Nozzle Configurations

Based on the trade-off structure revealed by the Pareto front, a representative optimized configuration was selected for further verification and comparative analysis. The objective of this section is to assess whether a design derived from the multi-objective optimization framework can achieve a balanced improvement in spray spreading performance while maintaining acceptable flow capacity, in comparison with the baseline and orthogonally optimized configurations. Table 5 summarizes the geometric parameters and corresponding performance indicators for the three nozzle configurations. The baseline structure exhibits a spray cone angle of 64.71° with a discharge coefficient of 0.2736. The orthogonal-optimized structure increases the spray cone angle to 68.52° , indicating improved spray spreading; however, this enhancement is accompanied by a reduction in discharge coefficient to 0.2465, indicating increased flow resistance. In contrast, the multi-objective-optimized structure achieves a spray cone angle of 69.14° while simultaneously increasing the discharge coefficient to 0.3903. Relative to the baseline configuration, this design exhibits enhanced spray spreading and an improvement in flow capacity, and compared with the orthogonal-optimized structure, it maintains a comparable spray cone angle while achieving a higher discharge coefficient. In quantitative terms, the discharge coefficient of the multi-objective optimized configuration increases by more than 40% relative to the baseline structure, while the spray cone angle shows an increase of approximately 4.4° .

Table 5. Comparison of dimensionless geometric parameters and performance indicators for different nozzle configurations.

Configuration	A	B	C	D	E	F	θ ($^\circ$)	C_d
Baseline structure	0.6	1	20	3	0.8	0.7	64.71	0.2736
Orthogonal-optimized structure	1.3	1.3	40	2.5	0.9	1.2	68.52	0.2465
Multi-objective optimized structure	1.2	0.8	20	4	0.8	1.2	69.14	0.3903

To further examine the reliability of the surrogate-assisted optimization results, the multi-objective-optimized configuration was re-evaluated using high-fidelity CFD simulations under the same operating conditions as those applied to the baseline and orthogonal-optimized structures. The simulation results confirm that the predicted spray cone angle and discharge coefficient are consistent with the values reported in Table 5. This improvement indicates that the multi-objective optimization implicitly favors geometric combinations that enhance internal flow stability and reduce excessive energy dissipation, rather than relying on extreme modification of a single parameter. Overall, the comparative results demonstrate that the proposed multi-objective optimization framework can effectively alleviate the trade-off between spray spreading and flow capacity, yielding a more balanced nozzle design. From an engineering perspective, achieving an enlarged spray cone angle while maintaining a relatively high discharge coefficient is beneficial for ensuring sufficient

mass flow rate without sacrificing spray coverage in practical injector systems. It should be noted that the numerical results are obtained within a limited range of nozzle geometries and operating conditions, and therefore should not be directly extrapolated to nozzles of different sizes. Nevertheless, the proposed framework and analysis methodology are general and can be applied to other nozzle scales by redefining the corresponding geometric parameters and operating conditions.

5. Conclusions

This study demonstrates that, under microgravity-relevant operating conditions, the spray cone angle and discharge coefficient of pressure swirl nozzles are governed by partially inconsistent geometric mechanisms, leading to an inherent conflict at the discrete parameter level. Within the investigated geometric ranges, comprehensive-weight-based orthogonal analysis is effective for identifying parameter influence trends and preliminary screening, but it is insufficient to achieve coordinated performance improvement when strong objective coupling exists.

By combining CFD-based surrogate modeling with Pareto-based multi-objective optimization, a representative compromise design was identified that achieves a simultaneous enhancement in spray spreading and flow capacity. Within the investigated geometric ranges, the discharge coefficient is increased by more than 40%, while the spray cone angle is enlarged by approximately 4.4° relative to the baseline configuration. These findings indicate that surrogate-assisted multi-objective optimization provides a more rational basis for nozzle design selection than conventional orthogonal weighting approaches in microgravity applications.

Overall, within the adopted numerical framework and microgravity-relevant operating conditions, the proposed integrated approach enables systematic parameter screening, quantitative trade-off analysis, and rational design selection for pressure swirl nozzles. The conclusions are limited to the applied modeling assumptions, geometric parameter ranges, and operating conditions considered in this study.

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Abbreviations

The following abbreviations are used in this manuscript:

AHP	Analytic Hierarchy Process
BP	Back Propagation (neural network)
BOI	Body of Influence
CFD	Computational Fluid Dynamics
CSF	Continuous Surface Force
CSES	Combustion Science Experimental System

CSS	China Space Station
NSGA-II	Non-dominated Sorting Genetic Algorithm II
RMSE	Root Mean Square Error
RNG	Renormalization Group (RNG k - ϵ model)
SIMPLE	Semi-Implicit Method for Pressure-Linked Equations
PRESTO!	Pressure Staggering Option
VOF	Volume of Fluid

Appendix A

Table A1. Orthogonal experimental design matrix and corresponding CFD-calculated results.

Case	A (d_3)	B (l_3/d_3)	C (α)	D (d_2/d_3)	E (d_1/d_3)	F (l_2/d_2)	θ ($^\circ$)	C_d
1	0.6	0.7	10	2.5	0.7	0.5	66.26	0.2466
2	0.6	1	20	3	0.8	0.7	64.71	0.2736
3	0.6	1.3	30	3.5	0.9	0.9	72.33	0.2127
4	0.6	1.6	40	4	1	1	72.97	0.1222
5	0.6	1.9	50	4.5	1.1	1.2	70.55	0.1232
6	0.6	2.2	60	5	1.2	1.5	67	0.2551
7	0.8	0.7	20	3.5	1	1.2	70.33	0.2454
8	0.8	1	30	4	1.1	1.5	74.17	0.2311
9	0.8	1.3	40	4.5	1.2	0.5	80.48	0.2214
10	0.8	1.6	50	5	0.7	0.7	67.94	0.1746
11	0.8	1.9	60	2.5	0.8	0.9	60.12	0.3783
12	0.8	2.2	10	3	0.9	1	60.2	0.2631
13	1	0.7	30	5	0.8	0.9	64	0.2547
14	1	1	40	2.5	0.9	1	65.4	0.2931
15	1	1.3	50	3	1	1.2	75.85	0.2067
16	1	1.6	60	3.5	1.1	1.5	60.73	0.2342
17	1	1.9	10	4	1.2	0.5	61.43	0.2389
18	1	2.2	20	4.5	0.7	0.7	66.14	0.2362
19	1.2	0.7	40	3	0.9	1	65.6	0.2204
20	1.2	1	50	3.5	1	1.2	72.35	0.2534
21	1.2	1.3	60	4	1.1	1.5	59.37	0.2329
22	1.2	1.6	10	4.5	1.2	0.5	76.79	0.1135
23	1.2	1.9	20	5	0.7	0.7	59.8	0.1688
24	1.2	2.2	30	2.5	0.8	0.9	65.4	0.2195
25	1.4	0.7	50	4	1.2	0.5	67.82	0.1843
26	1.4	1	60	4.5	0.7	0.7	71.54	0.1745
27	1.4	1.3	10	5	0.8	0.9	80.06	0.0828
28	1.4	1.6	20	2.5	0.9	1	63.75	0.2353
29	1.4	1.9	30	3	1	1.2	60.4	0.2715
30	1.4	2.2	40	3.5	1.1	1.5	64	0.2120
31	1.6	0.7	60	3.5	1.1	1.5	65.4	0.1558
32	1.6	1	10	4	1.2	0.5	72.6	0.1499
33	1.6	1.3	20	4.5	0.7	0.7	66.32	0.2453
34	1.6	1.6	30	5	0.8	0.9	61.58	0.1706
35	1.6	1.9	40	2.5	0.9	1	64.8	0.2399
36	1.6	2.2	50	3	1	1.2	62.29	0.1895

Note: A–F correspond to the design factors defined in Table 3.

Table A2. Weight distribution of different factor levels based on the spray cone angle.

Factor A	Weight	Factor B	Weight	Factor C	Weight	Factor D	Weight	Factor E	Weight	Factor F	Weight
ω_{A1}	0.0157	ω_{B1}	0.0418	ω_{C1}	0.0253	ω_{D1}	0.0325	ω_{E1}	0.0247	ω_{F1}	0.0270
ω_{A2}	0.0157	ω_{B2}	0.0441	ω_{C2}	0.0237	ω_{D2}	0.0328	ω_{E2}	0.0246	ω_{F2}	0.0251
ω_{A3}	0.0150	ω_{B3}	0.0455	ω_{C3}	0.0241	ω_{D3}	0.0341	ω_{E3}	0.0244	ω_{F3}	0.0256

Table A2. Cont.

Factor A	Weight	Factor B	Weight	Factor C	Weight	Factor D	Weight	Factor E	Weight	Factor F	Weight
ω_{A4}	0.0152	ω_{B4}	0.0423	ω_{C4}	0.0250	ω_{D4}	0.0344	ω_{E4}	0.0258	ω_{F4}	0.0249
ω_{A5}	0.0155	ω_{B5}	0.0395	ω_{C5}	0.0253	ω_{D5}	0.0364	ω_{E5}	0.0245	ω_{F5}	0.0261
ω_{A6}	0.0150	ω_{B6}	0.0403	ω_{C6}	0.0233	ω_{D6}	0.0337	ω_{E6}	0.0265	ω_{F6}	0.0248
$\bar{\omega}_A$	0.0154	$\bar{\omega}_B$	0.0422	$\bar{\omega}_C$	0.0245	$\bar{\omega}_D$	0.0340	$\bar{\omega}_E$	0.0251	$\bar{\omega}_F$	0.0256

Table A3. Weight distribution of different factor levels based on the discharge coefficient.

Factor A	Weight	Factor B	Weight	Factor C	Weight	Factor D	Weight	Factor E	Weight	Factor F	Weight
ω_{A1}	0.0210	ω_{B1}	0.0138	ω_{C1}	0.0141	ω_{D1}	0.0273	ω_{E1}	0.0172	ω_{F1}	0.0136
ω_{A2}	0.0459	ω_{B2}	0.0143	ω_{C2}	0.0214	ω_{D2}	0.0245	ω_{E2}	0.0157	ω_{F2}	0.0163
ω_{A3}	0.0693	ω_{B3}	0.0136	ω_{C3}	0.0207	ω_{D3}	0.0223	ω_{E3}	0.0201	ω_{F3}	0.0147
ω_{A4}	0.0824	ω_{B4}	0.0127	ω_{C4}	0.0214	ω_{D4}	0.0204	ω_{E4}	0.0186	ω_{F4}	0.0187
ω_{A5}	0.1077	ω_{B5}	0.0168	ω_{C5}	0.0185	ω_{D5}	0.0211	ω_{E5}	0.0164	ω_{F5}	0.0176
ω_{A6}	0.1395	ω_{B6}	0.0150	ω_{C6}	0.0193	ω_{D6}	0.0176	ω_{E6}	0.0144	ω_{F6}	0.0160
$\bar{\omega}_A$	0.0776	$\bar{\omega}_B$	0.0144	$\bar{\omega}_C$	0.0192	$\bar{\omega}_D$	0.0222	$\bar{\omega}_E$	0.0171	$\bar{\omega}_F$	0.0161

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