

Article

Oil-Phase Redistribution of Power-Law Fluids in a Guided-Vane Swirl Separator Under High-Temperature and High-Pressure Conditions

Beibei Kou ¹, Yaoyao Chen ² , Lixin Xu ¹, Xiaorong Li ¹ and Lele Yang ^{2,*}¹ College of Safety and Ocean Engineering, China University of Petroleum, Beijing 100100, China² School of Civil Engineering and Transportation, South China University of Technology, Guangzhou 510641, China

* Correspondence: yanglele@scut.edu.cn

Abstract

With offshore oil and gas exploration advancing into ultra-deep waters, subsea reservoirs face extreme high-temperature and high-pressure (HTHP) conditions, leading to significant alterations in crude oil properties. These changes destabilize oil–water phase distribution and impair separation efficiency in swirl separators. Non-Newtonian fluid behavior under HTHP conditions, characterized by temperature-sensitive viscosity and shear-thinning effects, further modifies the rheological response and internal swirling-flow characteristics. This study establishes a computational fluid dynamics (CFD) framework integrating Eulerian–Eulerian multiphase modeling with power-law rheology. Simulations analyzed power-law fluid behavior across a temperature range of 30–210 °C, focusing on shear-thinning effects and phase distribution. The results show that the oil-core transport fraction increased from 0.162 at 30 °C to 0.194 at 150 °C and then decreased to 0.141 at 210 °C. Although the peak tangential velocity continued to increase with temperature, the strongest central oil-phase enrichment occurred at 150 °C, indicating that stronger swirl does not necessarily correspond to stronger phase segregation. These results provide mechanistic insight and a numerical basis for subsequent scale-up studies of HTHP guided-vane swirl separators.

Keywords: high-temperature and high-pressure conditions; oil–water separation; non-Newtonian fluids; shear thinning; swirl separator

1. Introduction

With offshore oil and gas exploration advancing into ultra-deep waters (>3000 m), subsea reservoirs now face extreme conditions of up to 180 °C and 70 MPa, where temperature gradients reach 4 °C/100 m. These harsh environments intensify challenges in fluid management: crude oil exhibits increased density, viscosity, and water content while interfacial tension decreases significantly [1]. Such alterations destabilize oil–water phase distribution in swirl fields, promoting emulsification and compromising separation efficiency in subsea production systems.

At high temperature and pressure, crude oil exhibits increased density and viscosity, while interfacial tension between oil and water phases decreases sharply. These effects promote emulsification and hinder droplet coalescence, destabilizing the oil–water interface and reducing the efficiency of conventional separation technologies. Furthermore, the interaction between thermal and rheological properties gives rise to complex flow behaviors,



Academic Editor: Anton Vernet

Received: 30 August 2026

Revised: 15 September 2026

Accepted: 17 September 2026

Published: 21 September 2026

Copyright: © 2026 by the authors.

Licensee MDPI, Basel, Switzerland.

This article is an open access article distributed under the terms and conditions of the [Creative Commons Attribution \(CC BY\)](https://creativecommons.org/licenses/by/4.0/) license.

including temperature-dependent viscosity gradients, enhanced shear-thinning effects, and nonlinear vortex dynamics. These factors collectively modify the internal hydrodynamics of swirl-based separators, leading to unstable flow structures and fluctuating separation boundaries. From an engineering perspective, these phenomena significantly degrade the performance of subsea cyclonic separators designed for moderate conditions. Increased turbulence and reduced interfacial tension facilitate droplet breakup and redispersion, while temperature-induced density variations further complicate the flow field. Consequently, accurate modeling of high-temperature and high-pressure multiphase flow, with appropriate consideration of temperature-dependent rheological behavior, is essential for improving separation performance and ensuring stable operation in deep-sea production systems.

Recent studies have further advanced the understanding and optimization of guided-vane oil–water separation. Li et al. [2] investigated the separation performance of non-Newtonian oil–water flow in a swirl-vane separator using a Eulerian multiphase framework coupled with a power-law rheological model and demonstrated that oil concentration, inlet velocity, droplet size, density, and rheological parameters can significantly influence the separation response. Zhao et al. [3] subsequently proposed a spiral guide-vane hydrocyclone and showed that regulation of short-circuit flow provides an effective means of strengthening oil–water separation. More recently, a Eulerian–RSM–PBM framework has been applied to non-Newtonian polymer-containing oil–water flows in swirl-vane separators, revealing the coupled effects of viscosity, rheological index, inlet velocity, and oil content on separation behavior [4]. These studies demonstrate the growing interest in non-Newtonian guided-vane separation but have mainly focused on conventional operating conditions or variations in fluid and structural parameters.

The non-Newtonian behavior of reservoir fluids under high-temperature, high-pressure (HTHP) conditions critically impacts swirl separation efficiency through two mechanisms: temperature-sensitive viscosity variation and nonlinear stress response. Computational fluid dynamics (CFD) has emerged as an indispensable tool for modeling these complex interactions. Li et al. [5] established a validated CFD framework predicting shear stress evolution in drilling fluids at 180 °C, demonstrating <5% deviation from experimental measurements. This validation proves particularly relevant for modeling rotational flow fields in swirl separators. Awad et al. [6] further quantified the enhanced cuttings transport capacity of non-Newtonian fluids under HTHP conditions, revealing a 22% improvement in particle suspension stability compared to Newtonian counterparts during high-speed rotation. Meanwhile, the rheological behavior of non-Newtonian fluids under high-temperature and high-pressure conditions has received increasing attention. Recent experimental investigations have demonstrated that both temperature and pressure can substantially alter constitutive parameters, with temperature generally exerting the stronger influence on apparent viscosity and shear-thinning behavior [7,8]. Experimental measurements covering temperatures up to 210 °C and pressures above 170 MPa have further highlighted the strong dependence of rheological response on extreme operating conditions and the need for carefully calibrated constitutive models [7]. However, these HTHP rheological studies have primarily focused on wellbore hydraulics and drilling-fluid transport rather than centrifugal oil–water separation. The manner in which HTHP-dependent power-law rheology reorganizes the swirling flow field and oil-phase distribution in a guided-vane separator remains insufficiently understood. To address this gap, the present study investigates the oil-phase redistribution of power-law fluids in a guided-vane swirl separator under HTHP conditions. Rather than focusing on structural optimization or droplet population evolution, this study isolates the effect of temperature-dependent rheological variation on apparent viscosity, swirling intensity, axial transport, and central oil-phase enrichment.

The interplay between thermal and flow dynamics introduces additional complexity. Mebarki et al. [9] demonstrated that temperature gradients ≥ 3 °C/cm fundamentally alter viscoplastic fluid convection patterns, directly affecting separation interface stability. This thermal-flow coupling is further quantified through Mishra et al.'s [10] exponential rheological model, which correlates shear thinning ($n = 0.4$ – 0.7) with temperature-dependent viscosity reduction (15–30%) in nanofluids. Pressure-dominated flow regimes present distinct challenges—Bandyopadhyay and Das [11] identified 18–25% higher pressure drops at pipeline elbows for gas–liquid non-Newtonian mixtures versus single-phase flow, a critical design parameter for HTHP separation systems. Nanofluid studies reveal operational trade-offs: while 5 vol% Al_2O_3 nanoparticles enhance thermal conductivity by 12–18% (Shi et al. [12]), they concurrently increase viscosity 2.3-fold, disrupting boundary layer dynamics in plate heat exchangers (Rios-Irube et al. [13]). Xiong et al. [14] resolved this paradox by demonstrating that non-Newtonian behavior in Cu–water nanofluids boosts natural convection coefficients by 27% despite elevated viscosity, suggesting potential for thermal management in swirl separation devices. Agwu et al. [15] conducted a comprehensive review integrating laboratory experiments, field observations, and numerical simulations to elucidate mud rheological evolution under combined high-temperature and high-pressure conditions, highlighting its critical implications for swirl separation stability in extreme environments.

Under high-temperature and high-pressure conditions, non-Newtonian fluids exhibit pronounced nonlinear and temperature-dependent rheological behavior, which imposes greater demands on the modeling and optimization of processes such as swirling separation, mass transfer, and heat exchange. In recent years, computational fluid dynamics (CFD) has emerged as a powerful research tool, finding extensive application in simulating and analyzing the complex behavior of non-Newtonian fluids under such extreme conditions. Shekhar and Chakravorty [16] have successively conducted validation studies and engineering explorations on the applicability of various types of non-Newtonian constitutive models within CFD frameworks. Narasimha et al. [17] reviewed the hydrodynamics of non-Newtonian fluids in comminution and classification processes, with particular emphasis on the application of the Herschel–Bulkley model. Yang et al. [18] explored the effects of rheological properties on the internal flow field of cyclonic separators, further confirming the distinctive flow behavior of non-Newtonian fluids in such systems. Collectively, these studies provide a solid theoretical foundation for the application of non-Newtonian fluids in cyclonic separation systems and have significantly advanced technological developments in this field. Previous studies on axial and cylindrical swirling separators have further demonstrated that the internal vortex structure and velocity redistribution strongly govern oil-phase migration and phase segregation [19,20].

Despite these advances, critical gaps persist in HTHP swirl separation research. Current devices and online monitoring systems remain optimized for ambient conditions. Fundamental uncertainties surround HTHP effects on interfacial tension, viscoelastic film dynamics, and droplet kinetics. This study addresses these limitations through CFD simulations combining Eulerian multiphase modeling with power-law rheology, specifically investigating guided-vane swirl separators for polymer-flooded oil. Our results demonstrate the imperative of incorporating both HTHP-induced property variations and non-Newtonian behavior in separation process optimization.

2. Materials and Methods

2.1. Geometry and Operating Conditions

The computational domain consists of a guided-vane swirl separator followed by a straight separation section. The oil–water mixture enters the separator axially and

passes through the internal guide-vane assembly, where axial momentum is partially converted into tangential momentum. A strongly swirling flow is consequently generated downstream of the guide vanes, producing radial centrifugal segregation between the two liquid phases.

The main geometric parameters of the separator are summarized in Table 1. The cylindrical shell has a length of 2600 mm and an inner diameter of 100 mm. The guide-vane assembly consists of six blades arranged around a central hub. The front-cone, swirl-vane, and tail-cone sections have axial lengths of 88, 126, and 63 mm, respectively. The shell and hub diameters are 100 and 50 mm, and the swirl-vane outlet angle is 45° . The detailed dimensions and their geometric locations are illustrated in Figure 1 and summarized in Table 1.

Table 1. Dimensions of the guide-vane separator.

Structural Parameters	Symbol	Unit	Value
Shell length	L_0	mm	2600
Guide-vane front-cone length	L_1	mm	88
Swirl-vane length	L_2	mm	126
Guide-vane tail-cone length	L_3	mm	63
Shell diameter	D_0	mm	100
Hub diameter	D	mm	50
Swirl-vane outlet angle	α	$^\circ$	45

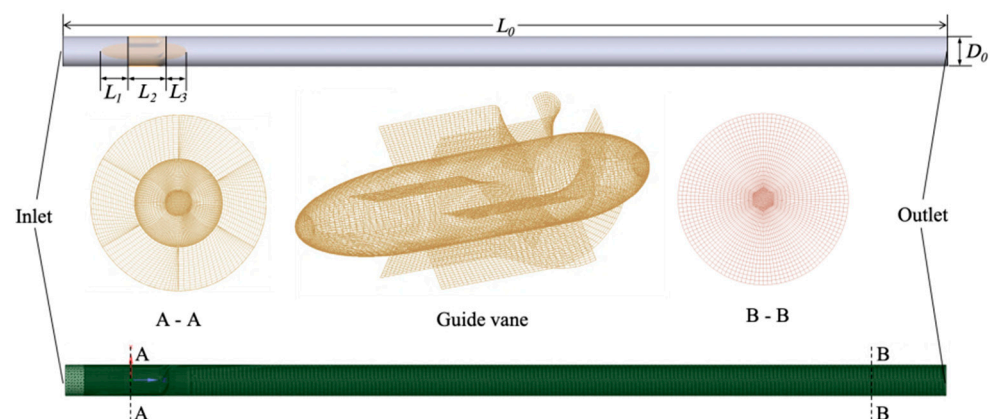


Figure 1. Computational geometry and mesh of the guided-vane swirl separator, including the overall flow domain, enlarged guide-vane assembly, representative cross-sectional meshes, and axial sampling locations A and B.

A velocity-inlet boundary condition was imposed at the inlet, whereas a pressure-outlet condition was applied at the downstream boundary. The inlet velocity and inlet oil volume fraction were maintained at 1.0 m/s and 10%, respectively, for all temperature cases. The oil-droplet diameter was also kept constant throughout the simulations so that the effects of temperature-dependent rheological variation could be isolated from those associated with droplet size.

Four representative temperature conditions, namely 30, 90, 150, and 210 $^\circ\text{C}$, were considered. The corresponding power-law rheological parameters were obtained from previously published high-temperature and high-pressure rheological measurements. All other geometric and operating parameters were maintained unchanged among the four cases, thereby allowing the influence of temperature-dependent rheology on the internal swirling flow and oil-phase redistribution to be assessed independently.

Each temperature was treated as an independent isothermal operating condition. No spatial temperature gradient or transient heat-transfer process was considered in the

present simulations. The temperature effect was introduced through the corresponding experimentally fitted power-law rheological parameters.

2.2. Computational Mesh and Grid Independence

A three-dimensional structured hexahedral mesh was generated using ICEM-CFD, with local refinement applied around the guide-vane assembly and in regions characterized by strong velocity gradients. The final computational mesh contains 1,557,350 cells.

Grid independence for the identical separator geometry was systematically assessed in our previous study using four structured meshes containing 426,347, 836,511, 1,557,350, and 2,235,872 cells. The pressure-drop predictions showed only minor variations after refinement from 1,557,350 to 2,235,872 cells. Considering both numerical accuracy and computational cost, the 1,557,350-cell mesh was selected and is retained in the present simulations. The corresponding grid-independence study is reported in Ref. [21].

2.3. Eulerian–Eulerian Multiphase Model

Eulerian–Eulerian modeling has been widely applied to oil–water two-phase transport and hydrocyclone separation, with interphase momentum transfer being particularly important for predicting dispersed-phase migration [22–24]. For phase q , the steady-state continuity equation can be written as:

$$\nabla \cdot (\alpha_q \rho_q \mathbf{u}_q) = 0, \quad (1)$$

where α_q , ρ_q , and $\mathbf{u}_q$ denote the volume fraction, density, and velocity vector of phase q , respectively.

The corresponding momentum equation is [24]:

$$\nabla \cdot (\alpha_q \rho_q \mathbf{u}_q \mathbf{u}_q) = -\alpha_q \nabla p + \nabla \cdot \boldsymbol{\tau}_q + \alpha_q \rho_q \mathbf{g} + \sum_{p \neq q} K_{pq} (\mathbf{u}_p - \mathbf{u}_q) + \mathbf{F}_q, \quad (2)$$

where p is the shared pressure, $\boldsymbol{\tau}_q$ is the stress tensor, K_{pq} represents the interphase momentum-exchange coefficient, and $\mathbf{F}_q$ includes additional interfacial forces considered in the multiphase formulation.

The phase-volume fractions satisfy:

$$\sum_{q=1}^N \alpha_q = 1. \quad (3)$$

The interphase momentum exchange is primarily governed by the relative motion between the oil and water phases. The drag coefficient was evaluated using the drag correlation adopted in our previous simulations of the same oil–water swirling system. The same multiphase-model settings were retained for all temperature cases to ensure that the observed variations originated primarily from the temperature-dependent rheological properties.

2.4. Power-Law Rheological Model

The non-Newtonian rheological behavior was described using the power-law constitutive model. The relationship between shear stress and shear rate is expressed as:

$$\tau = k \dot{\gamma}^n, \quad (4)$$

and the corresponding apparent viscosity is:

$$\mu_{app} = k\dot{\gamma}^{n-1}, \quad (5)$$

where k is the consistency index, n is the flow-behavior index, and $\dot{\gamma}$ is the local shear rate. For $n < 1$, the apparent viscosity decreases with increasing shear rate, corresponding to shear-thinning behavior.

The temperature-dependent power-law parameters adopted in this study were obtained from previously published HTHP rheological measurements conducted using a FANN iX77 HPHT rheometer (Fann Instrument Company, Houston, TX, USA). The corresponding parameters are summarized in Table 2.

Table 2. Temperature-dependent power-law rheological parameters used in the simulations.

Temperature	k (Pa·s ^{n})	n
30 °C	0.169	0.746
90 °C	0.020	0.901
150 °C	0.017	0.786
210 °C	0.306	0.295

It should be emphasized that the consistency index k cannot be interpreted independently as the apparent viscosity because μ_{app} is jointly determined by k , n , and the local shear rate. In particular, the combination of the relatively high consistency index and low flow-behavior index at 210 °C produces pronounced shear-thinning at elevated shear rates.

The experimental rheological measurements covered shear rates up to approximately 1022 s^{−1}. The power-law fit at 210 °C also exhibited a lower coefficient of determination than those obtained at lower temperatures. Therefore, predictions in localized CFD regions where the shear rate exceeds the experimental calibration range should be interpreted as extrapolations of the experimentally fitted constitutive relation. Accordingly, the 210 °C results are discussed as the response predicted by the adopted power-law representation rather than as direct evidence of thermal degradation or phase separation.

2.5. Reynolds Stress Turbulence Model

The internal flow generated by the guide vanes is characterized by strong rotation, streamline curvature, and turbulence anisotropy. The Reynolds stress model (RSM) was therefore adopted to describe the turbulent swirling flow.

Unlike two-equation eddy-viscosity models, RSM solves individual transport equations for the Reynolds-stress components and thus provides a more direct representation of turbulence anisotropy in strongly rotating flows. The general Reynolds-stress transport equation is expressed as [25]:

$$\frac{\partial}{\partial t}(\rho \overline{u_i' u_j'}) + \frac{\partial}{\partial x_k}(\rho u_k \overline{u_i' u_j'}) = P_{ij} + D_{ij} + \Pi_{ij} + \Omega_{ij} - \varepsilon_{ij}, \quad (6)$$

where P_{ij} represents production, D_{ij} diffusion, Π_{ij} pressure–strain redistribution, Ω_{ij} the contribution associated with system rotation, and ε_{ij} dissipation.

The RSM-based numerical framework has previously been applied to the same guided-vane separator geometry and has also been assessed for closely related strongly swirling oil–water flows. Therefore, the present study retains RSM in order to focus specifically on the influence of HTHP-dependent rheology rather than on turbulence-model sensitivity.

2.6. Numerical Methods

The governing equations were solved using ANSYS Fluent 2024R1 [26]. The pressure-based solver was employed, and pressure–velocity coupling was achieved using the phase-coupled SIMPLE algorithm. The Eulerian–Eulerian multiphase framework was employed to model both phases as continuous media. Boundary conditions comprised a pressure outlet and velocity inlet (1 m/s) with 10% oil volume fraction. The simulations adopted a pressure-based phase-coupled SIMPLE algorithm with first-order upwind spatial discretization for transient formulation. Convergence was monitored using scaled residuals, with an absolute convergence criterion of 1×10^{-4} for all governing equations, including continuity, momentum, turbulence, Reynolds-stress, and volume-fraction equations. Recent numerical studies of cyclone separators have similarly emphasized a sequential presentation of geometry, mesh generation, numerical methods, and flow-field analysis to improve the clarity and reproducibility of CFD investigations [27].

To address the strong nonlinearity of the oil–water two-phase flow, the phase-coupled SIMPLE (semi-implicit method for pressure-linked equations) algorithm was employed. By introducing an implicit, coupled solution of the phase-fraction transport equation, this method achieves a strong iterative coupling among the pressure, velocity, and phase-fraction fields. Compared with the conventional SIMPLE algorithm, the phase-coupled version embeds the phase-fraction continuity equation into the pressure-correction step, significantly enhancing convergence stability under conditions involving large density ratios. For spatial discretization, the first-order upwind scheme was adopted to enhance numerical robustness and convergence stability for the strongly coupled two-phase swirling flow. Although this scheme is more numerically diffusive than higher-order discretization schemes, it provided stable convergence for the present simulations. A second-order discretization sensitivity check was also performed for representative cases, and only minor differences were observed in the principal flow-field characteristics, while the computational cost increased substantially. Therefore, the first-order scheme was retained for the parametric simulations. The scalar field gradients were reconstructed using the least-squares cell-based gradient method, which provides superior numerical robustness when applied to unstructured meshes. Pressure interpolation was performed using the body-force-weighted scheme, in which a body-force weighting factor is applied to correct the face pressure gradient. This approach effectively suppresses numerical oscillations in flow fields dominated by strong centrifugal forces, thereby improving the stability and accuracy of the overall simulation.

Time step $\Delta t = 0.002$ s was used, together with under-relaxation factors (pressure 0.3, momentum 0.7, turbulence quantities 0.8) to guarantee iterative stability. The solver employs the phase-coupled SIMPLE algorithm for pressure–velocity coupling, first-order upwind spatial discretization, and least-squares cell-based gradient reconstruction.

Standard wall functions were employed to bridge the near-wall region between the viscous sublayer and the fully turbulent core flow. The first near-wall cell was positioned within the logarithmic region, with a target value of approximately $30 < y^+ < 300$. The dimensionless wall distance is defined as:

$$y^+ = \frac{u_\tau y_1}{\nu}, \quad (7)$$

where u_τ is the friction velocity, y_1 is the distance from the wall to the center of the first near-wall cell, and ν is the kinematic viscosity. Accordingly, the first-cell distance can be estimated as:

$$y_1 = \frac{y^+ \nu}{u_\tau}. \quad (8)$$

For a smooth wall, the dimensionless mean velocity in the logarithmic region follows the conventional log-law relationship:

$$u^+ = \frac{1}{\kappa} \ln(y^+) + B, \quad (9)$$

where $u^+ = u/u_\tau$, $\kappa = 0.41$ is the von Kármán constant, and $B = 5.2$ is the empirical intercept for a smooth wall.

The turbulence quantities at the near-wall cell were specified consistently with the standard wall-function formulation. The turbulent kinetic energy is related to the friction velocity by:

$$k = \frac{u_\tau^2}{\sqrt{C_\mu}}, \quad (10)$$

and the turbulence dissipation rate is evaluated from the local equilibrium relation:

$$\varepsilon = \frac{u_\tau^3}{\kappa y_1}, \quad (11)$$

where $C_\mu = 0.09$ is the turbulence-model constant. These relations provide consistency among the wall shear stress, logarithmic velocity profile, turbulent kinetic energy, and turbulence dissipation rate in the near-wall region.

2.7. Model Validation

The numerical framework employed in the present study has been validated in previous investigations. For the same guided-vane separator geometry, the Eulerian–Eulerian multiphase model coupled with the power-law rheological model and RSM was previously applied to non-Newtonian swirling flow, and the predicted tangential-velocity distributions showed satisfactory agreement with reference data [21].

In addition, a closely related Eulerian–RSM numerical framework developed by our group was subsequently evaluated against experimental oil–water separation data [2]. In that study, the predicted oil-phase outlet mass flow rates were compared with experimental measurements over a range of inlet velocities, and satisfactory agreement in both the variation trend and quantitative values was obtained. This comparison provides an experimental basis for the applicability of the present multiphase-flow framework.

The temperature-dependent rheological parameters adopted in the present simulations were obtained independently from HTHP rheological experiments using a Fann IX77 rheometer [7]. The corresponding numerical rheological predictions were also compared with experimental viscosity measurements and showed satisfactory agreement [7]. Taken together, these previous flow-field, separation-performance, and rheological validations provide the numerical and experimental basis for the present simulations.

3. Results and Discussion

3.1. Oil-Phase Distribution

The four isothermal cases were compared to evaluate how temperature-dependent power-law rheology influences oil-phase redistribution and the internal swirling-flow structure. Because all geometric and operating parameters were maintained unchanged, the differences among the cases primarily reflect the effects of the prescribed rheological parameters corresponding to 30, 90, 150, and 210 °C.

To quantify the degree of central oil-phase concentration, the oil-core transport fraction was defined as:

$$F_o(R_c) = \frac{\int_0^{R_c} \alpha_o u_z 2\pi r dr}{\int_0^R \alpha_o u_z 2\pi r dr}, \quad (12)$$

where R is the pipe radius and $R_c = 0.3R$ denotes the prescribed oil-core region. $\overline{\alpha_o}(r)$ and $\overline{u_z}(r)$ are the radially averaged oil volume fraction and axial velocity, respectively, obtained by averaging the values on the two sides of the pipe centerline. A larger F_o indicates a greater fraction of oil-phase transport concentrated in the central region.

Figure 2 gives the axial distribution of oil volume fraction in a swirl separator for power-law fluids under varying temperatures. The oil phase exhibits periodic accumulation along the axial direction, reflecting the helical flow characteristics inherent to the swirl field. As temperature increases, reduced fluid viscosity and intensified shear-thinning behavior promote centrifugal migration of oil droplets toward the central region. At 30 °C, higher viscosity impedes phase mobility, resulting in relatively uniform oil dispersion. The strongest central oil-phase enrichment occurs at 150 °C, where concentrated oil-core formation is observed. However, at 210 °C, dispersed oil patterns emerge, likely attributable to oil-phase redistribution under excessive shear rates and turbulence amplification.

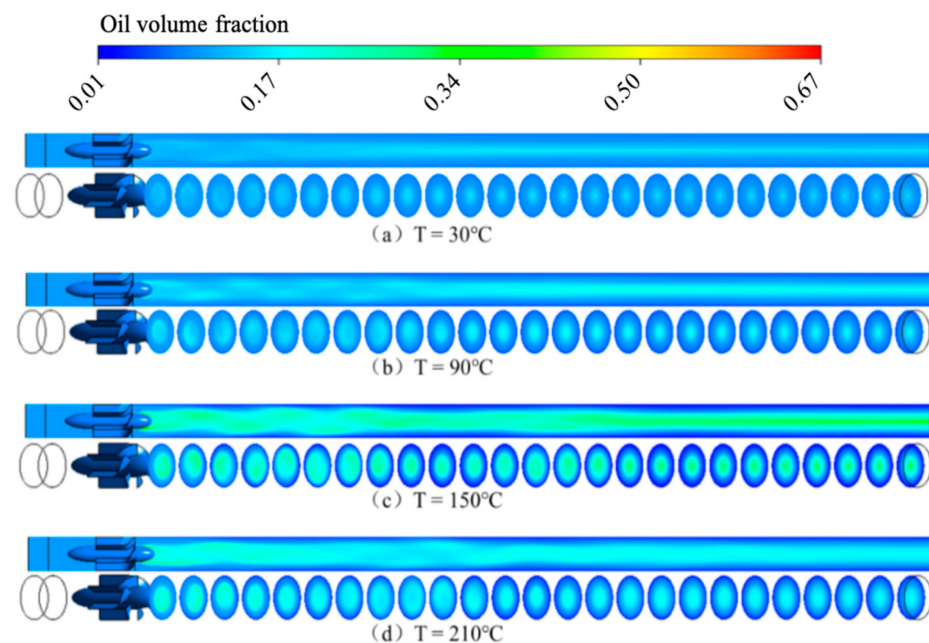


Figure 2. Contour plots of oil volume fraction distribution along the axial direction for power-law fluids at different temperatures.

In addition, Figure 2 further reveals a periodic redistribution pattern of the oil phase along the separator, which is associated with the helical structure of the swirling flow. The mixture enters axially and acquires tangential momentum while passing through the guide-vane assembly, thereby generating a swirling flow downstream. At lower temperatures, the higher viscosity weakens flow inertia, leading to a more stable internal flow structure. In contrast, under high-temperature conditions, the reduced viscosity and increased shear strain rate enhance the swirling separation effect, thereby altering the axial distribution of the oil phase.

To supplement the qualitative interpretation based on the oil-volume-fraction contours, the oil-core transport fraction $F_o(0.3R)$ was further evaluated at $z = 2400$ mm, as shown in Figure 3. The calculated values are 0.162, 0.166, 0.194, and 0.141 at 30, 90, 150, and 210 °C, respectively. Only a slight increase is observed from 30 to 90 °C, whereas $F_o(0.3R)$ increases markedly to its maximum value of 0.194 at 150 °C. This indicates that approximately 19.4%

of the axial oil-phase transport is concentrated within the central region $r \leq 0.3R$ under this condition.

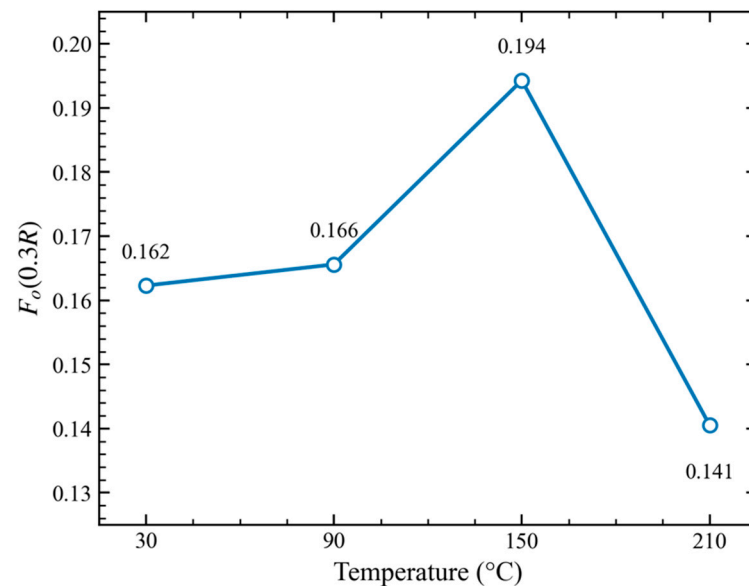


Figure 3. Oil-core transport fraction $F_o(0.3R)$ at $z = 2400$ mm under different temperatures.

In contrast, $F_o(0.3R)$ decreases substantially to 0.141 at 210 °C. This result is particularly noteworthy because the 210 °C case exhibits the highest tangential-velocity magnitude, as discussed subsequently. Therefore, an increase in swirl intensity does not necessarily lead to stronger central oil-phase segregation. The quantitative $F_o(0.3R)$ results are consistent with the oil-volume-fraction distributions and demonstrate that the most pronounced central oil enrichment occurs at the intermediate temperature of 150 °C rather than at the highest temperature. The separation-related behavior should therefore be interpreted as the combined response of centrifugal migration, temperature-dependent rheology, and oil-phase redistribution rather than solely as a consequence of increasing swirl intensity.

Figure 4 delineates the radial variation of oil phase distribution at the outlet cross-section. The intermediate-temperature cases exhibit more pronounced central oil-phase enrichment, characterized by a distinct peak in the central volume fraction. At 30 °C, the distribution curve exhibits a lower and smoother central peak, indicating weaker central oil-phase concentration. This is attributed to the elevated viscosity of the power-law fluid under low-temperature conditions, which weakens shear effects and suppresses swirl-driven phase stratification. Notably, the peak intensity decreases at 210 °C despite further viscosity reduction, accompanied by a flattened radial profile. This reduction in central oil-phase concentration may be associated with the elevated local shear rate. The elevated local shear rate may enhance turbulent dispersion and weaken the central concentration of the oil phase.

3.2. Apparent Viscosity and Rheological Response

The apparent viscosity of power-law fluids is characterized by a coupled dependence on both temperature and shear strain rate. As the temperature increases from 30 to 150 °C, the fitted rheological parameters change substantially, resulting in a general reduction in apparent viscosity over the investigated shear-rate range. Figure 5 presents the apparent-viscosity distributions over the entire computational domain, whereas Figure 6 shows the corresponding radial profiles extracted only at the downstream section $z = 2400$ mm. Therefore, the viscosity ranges shown in the two figures are not expected to be identical. The larger local values visible in Figure 5 occur in localized low-shear regions within the full

separator, while the downstream flow at $z = 2400$ mm has already undergone substantial shear development and exhibits a considerably narrower apparent-viscosity range. Figure 6 should therefore be interpreted as a sectional quantitative characterization of the global distributions shown in Figure 5 rather than as a reproduction of their full-domain extrema.

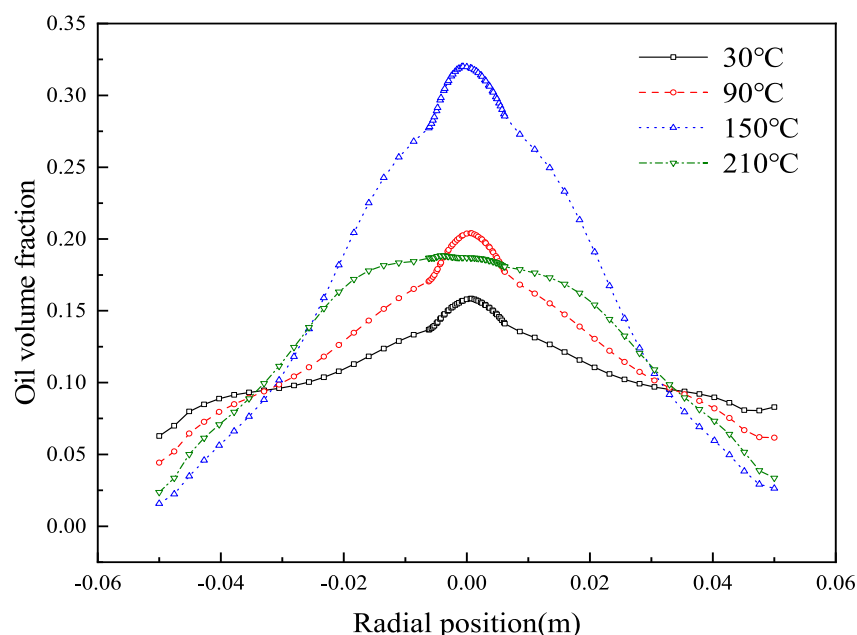


Figure 4. Oil volume fraction distribution at $z = 2400$ mm for power-law fluids at different temperatures.

The rheological parameters of the power-law fluid, derived from simulations across four temperature regimes (30–210 °C), reveal critical insights into the interplay between temperature and shear-thinning behavior (Figure 6). At 30 °C, the highest consistency index ($k = 0.169$) and moderate flow index ($n = 0.746$) indicate pronounced non-Newtonian behavior, with high apparent viscosity dominating the core region. This elevated viscosity suppresses droplet mobility, resulting in uniform dispersion and weak centrifugal recirculation, which undermines oil–water segregation.

As the temperature increases to 90 °C and 150 °C, the viscosity peak in the central region decreases markedly, and the radial distribution becomes smoother. The elevated shear strain rate enhances the shear-thinning response of the fluid, leading to a more uniform viscosity field at the outlet. Under these conditions, the swirling structure exhibits greater stability, facilitating a more effective oil–water separation process. A temperature of 150 °C provides the most favorable condition for central oil-phase concentration among the investigated cases. At 210 °C, the fitted rheological parameters change markedly to $k = 0.306$ and $n = 0.295$, producing a pronounced shear-thinning response. These parameters were obtained from previously published HTHP rheological measurements rather than from the present CFD calculations. Importantly, the consistency index should not be interpreted independently as the apparent viscosity, because the power-law viscosity is jointly determined by k , n , and the local shear rate according to $\mu_{app} = k\dot{\gamma}^{n-1}$. The low value of n therefore produces a pronounced shear-thinning response at 210 °C, especially in regions of elevated shear rate.

It should also be noted that the power-law fit at 210 °C has a lower coefficient of determination than that at the lower temperatures, indicating increased uncertainty in representing the rheology at this extreme temperature using a simple power-law model. Accordingly, the present results at 210 °C are interpreted as the response predicted by the experimentally fitted power-law representation, rather than as direct evidence of thermal degradation or phase separation.

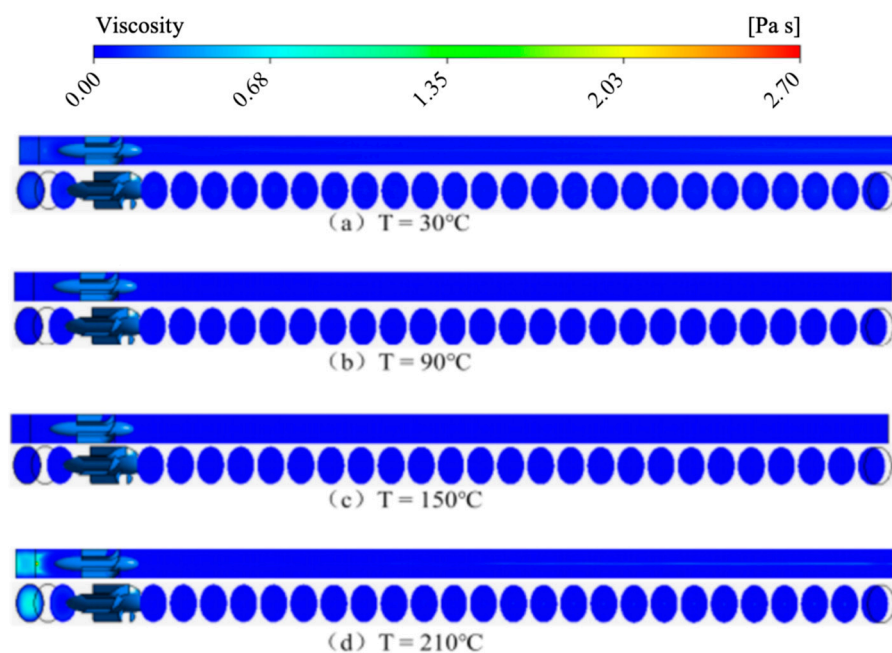


Figure 5. Apparent-viscosity contours in the entire guided-vane swirl separator at different temperatures. The radial profiles shown in Figure 6 are extracted at $z = 2400$ mm.

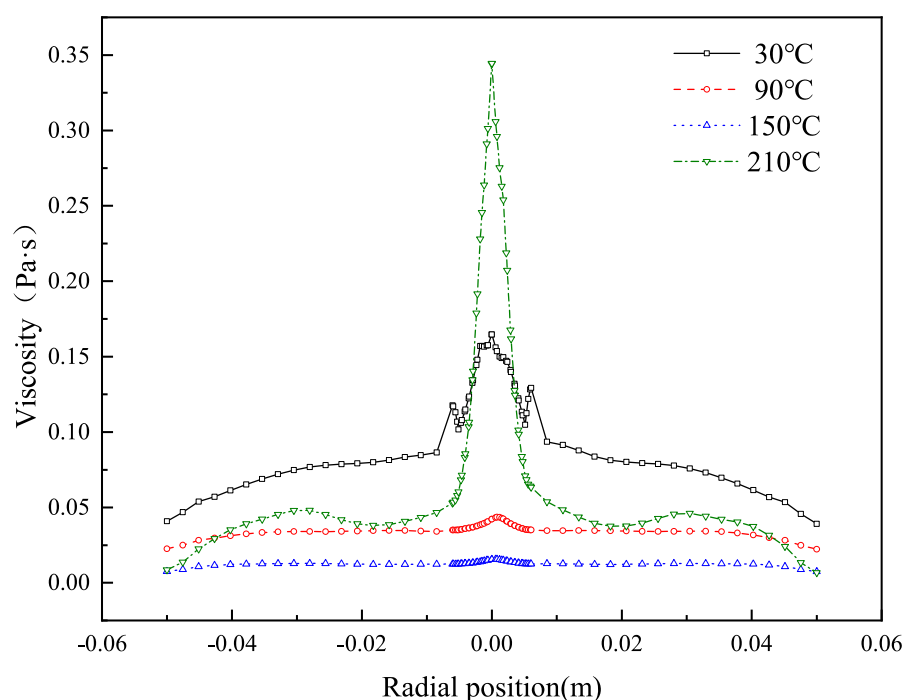


Figure 6. Radial distributions of apparent viscosity at $z = 2400$ mm for power-law fluids at different temperatures.

As the temperature increases to 90–150 °C, the changes in the rheological parameters lead to substantially lower apparent viscosity and enhanced shear sensitivity, resulting in flatter radial viscosity profiles. The reduced apparent viscosity facilitates oil-phase redistribution and promotes the formation of a concentrated oil-core region. Among the investigated cases, the 150 °C condition exhibits the most favorable viscosity distribution for central oil-phase enrichment. At 210 °C, however, the apparent-viscosity profile becomes highly localized near the pipe center. This distinct response reflects the strong shear dependence of the experimentally fitted power-law model at this temperature. The

localized high-shear regions may further enhance turbulent dispersion and modify the spatial redistribution of the oil phase, thereby weakening the stability of the concentrated oil-core structure.

3.3. Velocity and Pressure Fields

Figure 7 shows that at 30 °C, high fluid viscosity and weak shear-thinning led to a parabolic axial velocity distribution, with maximum speed at the center. This indicates strong axial flow and weak swirl recirculation, limiting central oil-phase segregation. As temperature increased to 90–150 °C, enhanced shear-thinning reduced central viscosity, flattening the axial velocity profile. Stronger swirl recirculation suppressed mainstream flow, promoting oil-phase redistribution. At 210 °C, extremely low viscosity weakened non-Newtonian behavior, causing turbulent dispersion. This highlights the critical role of temperature in balancing shear-thinning and turbulence to optimize separation.

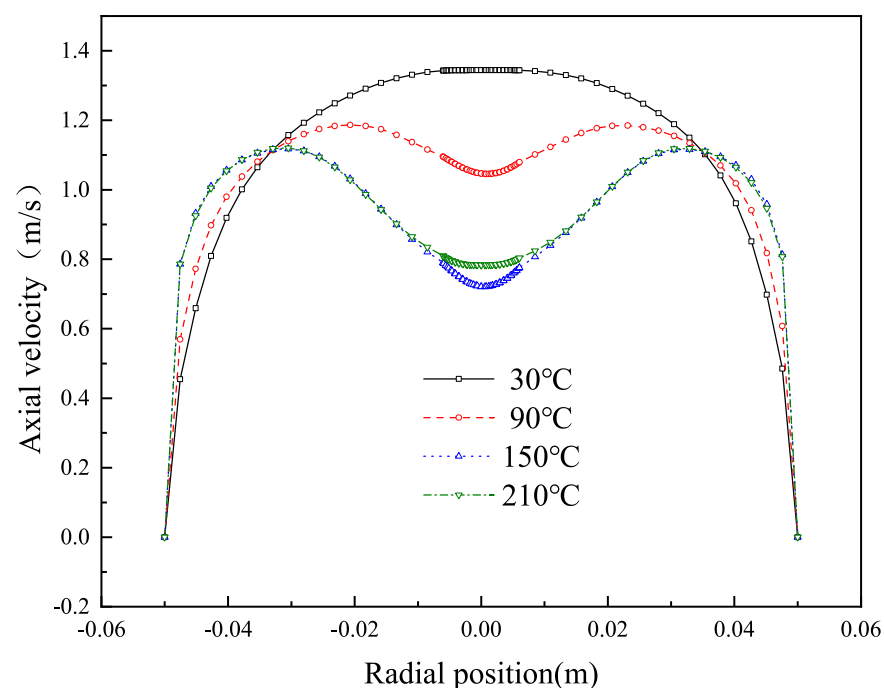


Figure 7. Axial velocity distribution at $z = 2400$ mm for power-law fluids at different temperatures.

Figure 8 presents the radial distributions of tangential velocity at $z = 2400$ mm under different temperatures. A pronounced double-peak profile is observed for all cases, with the tangential velocity approaching zero near the pipe center and increasing in magnitude toward off-axis locations before decreasing again near the wall. As the temperature increases, the rotational motion is progressively intensified. The peak magnitude of the tangential velocity increases from 0.257 m/s at 30 °C to 0.371 m/s at 90 °C, 0.396 m/s at 150 °C, and 0.469 m/s at 210 °C. Meanwhile, the radial location of the peak generally shifts outward, indicating an expansion of the high-swirl region. This trend suggests that the temperature-dependent rheological variation reduces the resistance to rotational motion and promotes angular-momentum redistribution within the separator.

However, stronger swirl does not necessarily correspond to better oil–water separation. Although the 210 °C case exhibits the largest tangential-velocity magnitude, the oil-phase distribution shown in Figures 3 and 4 becomes less concentrated than that at 90–150 °C. This indicates that excessive rotational intensity at the highest temperature does not further improve phase segregation and may instead enhance turbulent mixing and destabilize the concentrated oil-core structure. Therefore, the separation response is governed by

a balance between sufficiently strong centrifugal transport and excessive shear-induced mixing, with the intermediate-temperature range of 90–150 °C providing a more favorable hydrodynamic condition for oil-phase concentration.

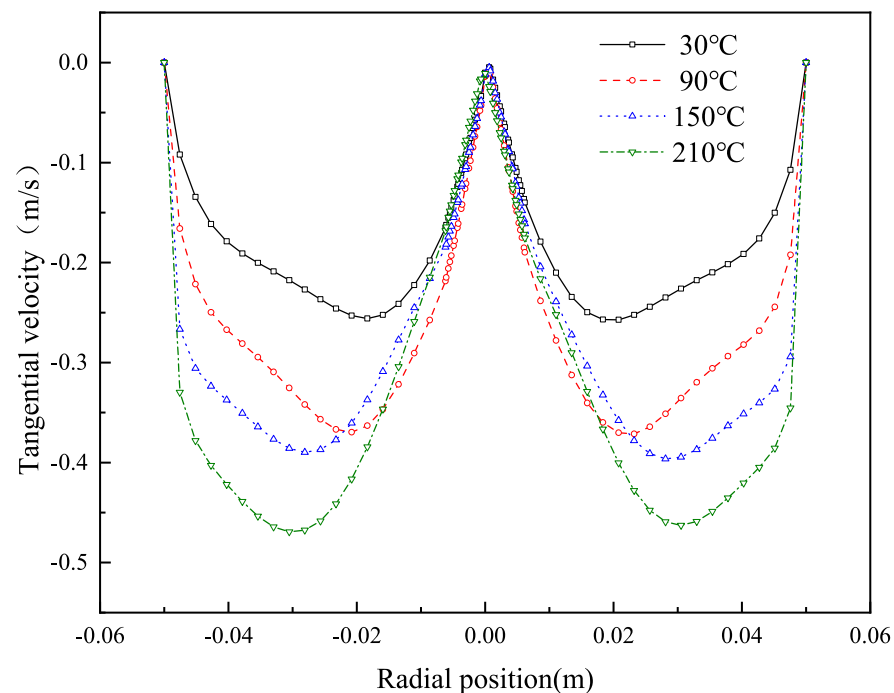


Figure 8. Tangential velocity distribution at $z = 2400$ mm for power-law fluids at different temperatures.

Figure 9 shows that the radial velocity of the fluid exhibits a typical “antisymmetric” distribution, with a sharp jump near the central axis, reflecting the strong influence of swirl-induced motion. At 30 °C, the high viscosity and weak shear-thinning effect suppress vortex development, resulting in smaller radial velocity amplitudes and limited oil-phase redistribution. As the temperature increases to 90 °C and 150 °C, the fluid viscosity decreases and the shear strain rate rises, allowing the vortex structure to develop more fully. Consequently, the radial velocity increases significantly, promoting enhanced radial migration of the oil phase and more pronounced central oil-phase enrichment. However, at 210 °C, although the radial velocity remains relatively strong, the distribution curve near the center becomes smoother. This behavior may be attributed to intensified turbulent mixing, which weakens the stability of the concentrated oil-core structure.

Figure 10 presents the axial velocity distribution of the power-law fluid at different temperatures. Overall, the internal flow structure of the swirling separator exhibits a distinct periodic pattern, particularly within the swirling chamber and downstream flow channel, where the velocity field shows a regular, wave-like distribution. This periodic velocity variation originates from the inherent characteristics of swirling flow, in which the fluid develops a helical motion under the combined effects of centrifugal force and the internal recirculation zone.

At the low-temperature condition of 30 °C, the fluid exhibits high initial viscosity and a weak shear-thinning effect, resulting in lower axial velocity and a less stable swirling structure. As shown in Figure 10, the velocity decays rapidly within the separator, which may weaken the stability of the swirl field and consequently reduce oil–water separation efficiency. With increasing temperature (90 °C and 150 °C), the axial velocity rises progressively, and the periodic distribution of the swirling structure becomes more pronounced. The enhanced shear-thinning effect and intensified turbulence at these temperatures lead to steeper velocity gradients and stronger swirling motion, promoting oil-phase redistribution

and improving separation performance. At 210 °C, the axial velocity reaches its maximum, and the overall flow field becomes more uniform. This indicates that the flow resistance is further reduced and the swirl structure achieves higher stability under high-temperature conditions. However, the intensified turbulent mixing at 210 °C may enhance oil-phase dispersion in high-shear regions and weaken the concentrated oil-core structure.

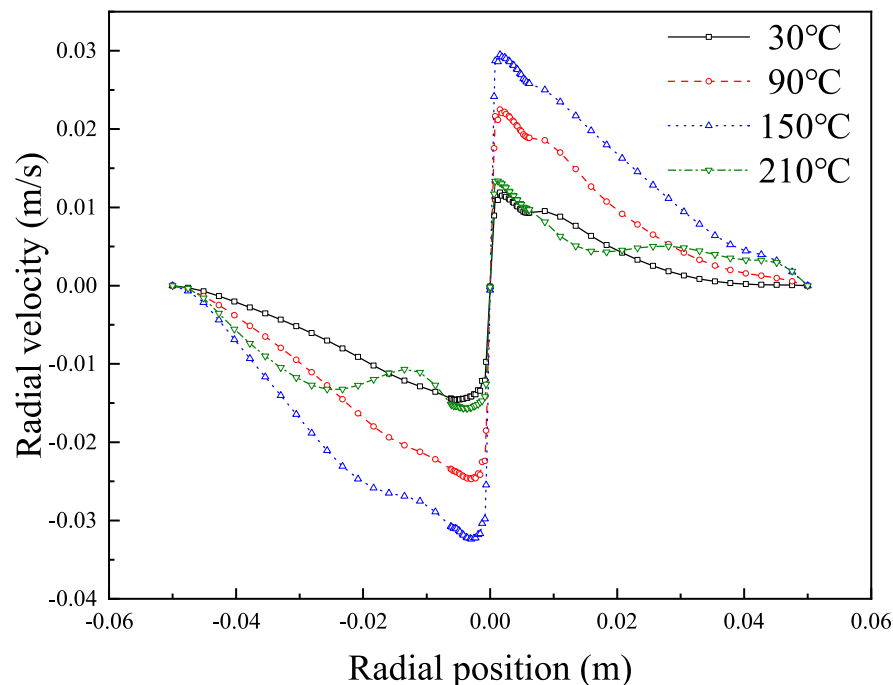


Figure 9. Radial velocity distribution at $z = 2400$ mm for power-law fluids at different temperatures.

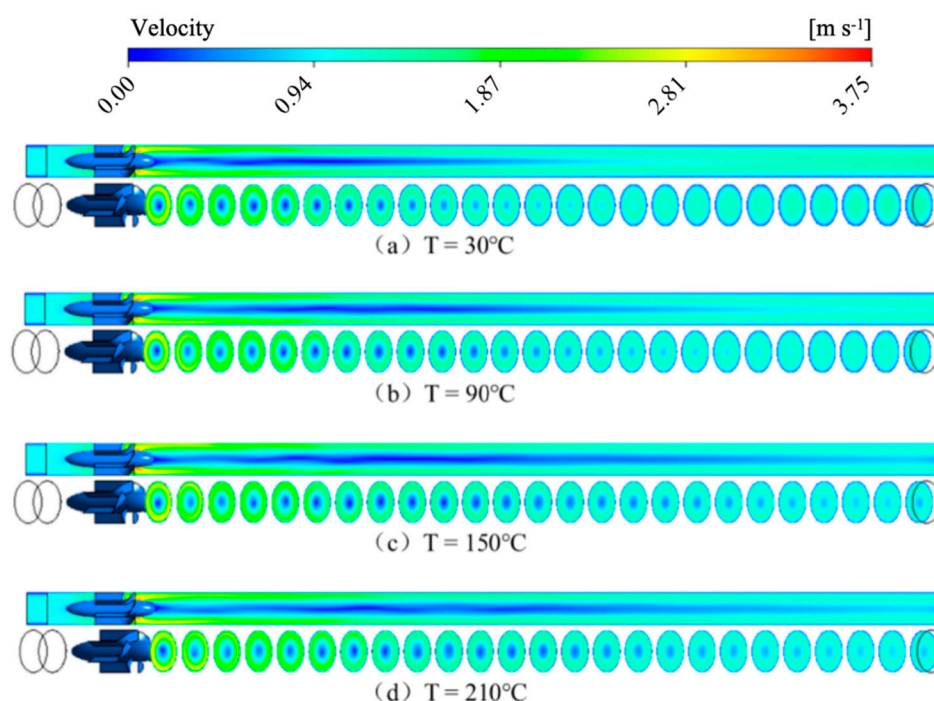


Figure 10. Contour plots of axial velocity distribution along the axial direction for power-law fluids at different temperatures.

Under different temperature conditions, the pressure distribution at the swirling outlet exhibits a typical “U”-shaped trend (Figure 11). The pressure is lowest in the central region

and higher in the radial outer region. This distribution characteristic primarily stems from the rotational flow structure inside the swirling. When the fluid enters the swirling flow, it forms a vortex due to the tangential velocity. Centrifugal force causes the fluid to be thrown toward the wall, resulting in a low-pressure zone in the central region of the swirling flow and a high-pressure zone near the wall area. This phenomenon is consistent with the typical Rankine vortex structure [28].

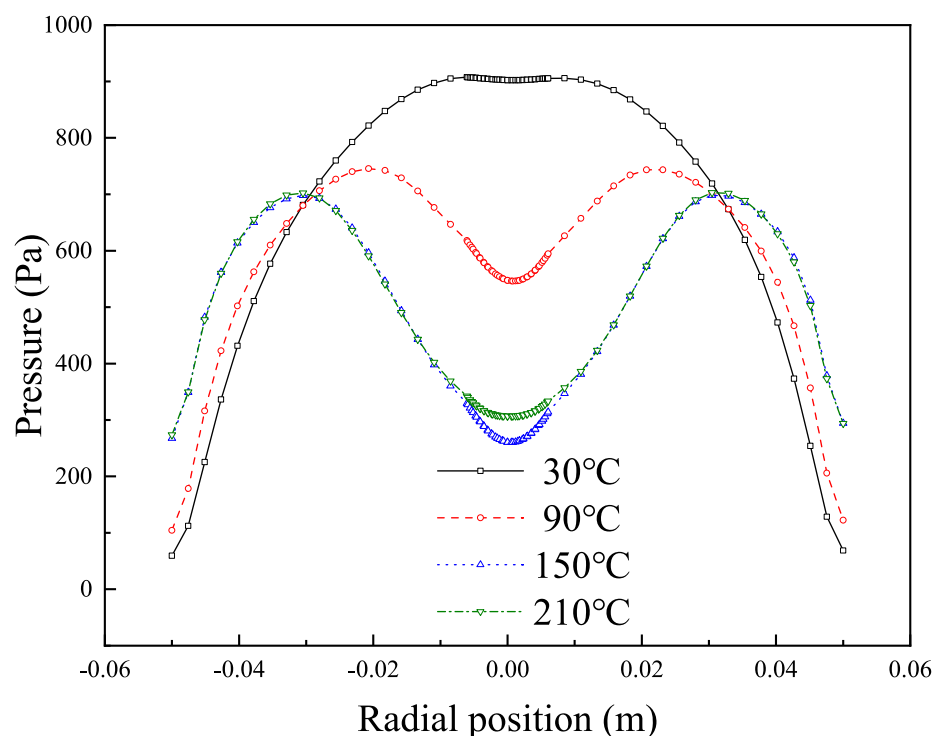


Figure 11. Pressure distribution at $z = 2400$ mm for power-law fluids at different temperatures.

4. Conclusions

This study investigated the influence of temperature-dependent power-law rheology on the swirling-flow structure and oil-phase redistribution in a guided-vane separator using four independent isothermal conditions. The major findings are summarized as follows:

The oil-core transport fraction $F_o(0.3R)$ provides a quantitative measure of central oil-phase segregation. Its value increased from 0.162 at 30 °C to a maximum of 0.194 at 150 °C, before decreasing to 0.141 at 210 °C. This behavior demonstrates that the strongest swirl does not necessarily produce the strongest central oil enrichment and that the intermediate-temperature condition provides a more favorable balance for oil-phase concentration.

The axial velocity profiles evolved from a parabolic shape at 30 °C to a more flattened distribution between 90 and 150 °C, indicating enhanced recirculation and oil-core stabilization in the separator. The peak magnitude of the tangential velocity increased progressively from 0.257 m/s at 30 °C to 0.469 m/s at 210 °C, indicating enhanced rotational motion at elevated temperatures. However, the strongest swirl at 210 °C did not yield the highest oil-phase concentration, demonstrating that separation performance depends on a balance between centrifugal transport and excessive shear-induced mixing rather than on swirl intensity alone.

The present results should be interpreted as mechanistic and numerical evidence for the influence of HTHP-dependent rheology on oil–water swirling flow rather than as direct industrial-scale design criteria. Further scale-up studies should systematically examine Reynolds-number effects, residence time, flow-rate range, and geometric similarity before the present findings are extended to full-scale subsea separators.

Author Contributions: Conceptualization, L.Y. and L.X.; methodology, B.K. and Y.C.; software, B.K.; validation, B.K., Y.C. and X.L.; formal analysis, B.K.; investigation, B.K. and Y.C.; resources, L.Y. and L.X.; data curation, B.K.; writing—original draft preparation, B.K.; writing—review and editing, Y.C., L.X., X.L. and L.Y.; visualization, B.K.; supervision, L.Y. and L.X.; project administration, L.Y.; funding acquisition, L.Y. and L.X. All authors have read and agreed to the published version of the manuscript.

Funding: This research was funded by the Key Technologies Program of Nansha District (grant No. 2023ZD011).

Data Availability Statement: The data presented in this study are available on request from the corresponding author. The data are not publicly available due to ongoing research.

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

Abbreviations

The following abbreviations are used in this manuscript:

CFD	Computational fluid dynamics
HTHP	High-temperature and high-pressure
RANS	Reynolds-averaged Navier–Stokes
RSM	Reynolds stress model
SIMPLE	Semi-implicit method for pressure-linked equations

Nomenclature

The following symbols are used in this manuscript:

Symbol	Definition	Unit
α_q	Volume fraction of phase q	–
ρ_q	Density of phase q	$\text{kg}\cdot\text{m}^{-3}$
$\mathbf{u}_q$	Velocity vector of phase q	$\text{m}\cdot\text{s}^{-1}$
p	Pressure	Pa
$\boldsymbol{\tau}_q$	Stress tensor of phase q	Pa
K_{pq}	Interphase momentum-exchange coefficient	$\text{kg}\cdot\text{m}^{-3}\cdot\text{s}^{-1}$
$\mathbf{F}_q$	Additional interfacial/body force	$\text{N}\cdot\text{m}^{-3}$
k	Power-law consistency index	$\text{Pa}\cdot\text{s}^n$
n	Power-law flow-behavior index	–
$\dot{\gamma}$	Shear rate	s^{-1}
μ_{app}	Apparent viscosity	$\text{Pa}\cdot\text{s}$
u_τ	Friction velocity	$\text{m}\cdot\text{s}^{-1}$
y_1	Distance from wall to first near-wall cell center	m
y^+	Dimensionless wall distance	–
u^+	Dimensionless velocity	–
ν	Kinematic viscosity	$\text{m}^2\cdot\text{s}^{-1}$
κ	von Kármán constant	–
B	Log-law intercept	–
C_μ	Turbulence-model constant	–
k_t	Turbulent kinetic energy	$\text{m}^2\cdot\text{s}^{-2}$
ε	Turbulence dissipation rate	$\text{m}^2\cdot\text{s}^{-3}$
R	Pipe radius	m
R_c	Prescribed oil-core radius	m
$F_o(R_c)$	Oil-core transport fraction	–
$\bar{\alpha}_o$	Radially averaged oil volume fraction	–
$\bar{u}_z$	Radially averaged axial velocity	$\text{m}\cdot\text{s}^{-1}$

L_0	Shell length	mm
L_1	Guide-vane front-cone length	mm
L_2	Swirl-vane length	mm
L_3	Guide-vane tail-cone length	mm
D_0	Shell diameter	mm
D	Hub diameter	mm
α	Swirl-vane outlet angle	°

References

- Liu, J.; Liu, S.; Zhong, L.; Yuan, S.; Wang, Q.; Wei, C. Study on the emulsification characteristics of heavy oil during chemical flooding. *Phys. Fluids* **2023**, *35*, 053330. [\[CrossRef\]](#)
- Li, B.; Huang, C.; Liu, L.Y.; Yao, L.; Ning, B.; Yang, L. Separation efficiency prediction of non-Newtonian oil-water swirl-vane separators in offshore platform based on GA-BP neural network. *Ocean Eng.* **2024**, *296*, 116984. [\[CrossRef\]](#)
- Zhao, W.; Li, J.-P.; Zhang, T.; Wei, A.; Li, S.-Y.; Zhao, Z.-H.; Yang, X.-J.; Jiang, X.; Wang, H.-L. Strengthened oil-water separation by swirl vane hydrocyclone based on short-circuit flow regulation. *J. Water Process Eng.* **2024**, *65*, 105773. [\[CrossRef\]](#)
- Li, B.; Yang, L.; Zhang, K.; Zhu, P.; Zeng, Y. Oil-water separation behavior in cyclonic flow of non-Newtonian polymer fluids. *Chem. Eng. Res. Des.* **2025**, *220*, 134–147. [\[CrossRef\]](#)
- Li, Z.; Zheng, L.; Huang, W. Rheological analysis of Newtonian and non-Newtonian fluids using Marsh funnel: Experimental study and computational fluid dynamics modeling. *Energy Sci. Eng.* **2020**, *8*, 2054–2072. [\[CrossRef\]](#)
- Awad, A.M.; Hussein, I.A.; Nasser, M.S.; Ghani, S.A.; Mahgoub, A.O. A CFD-RSM study of cuttings transport in non-Newtonian drilling fluids: Impact of operational parameters. *J. Pet. Sci. Eng.* **2022**, *208*, 109613. [\[CrossRef\]](#)
- Yang, L.-L.; Li, B.; Chen, X.-D.; Yu, L.; Jing, F.-M.; Zhang, D. Experimental and numerical study of the rheological characteristics and flow law of water-based drilling fluids in high-temperature and high-pressure wellbores. *J. Hydrodyn.* **2024**, *36*, 954–967. [\[CrossRef\]](#)
- Sun, X.; Qiu, S.; Wang, J.; Wang, Z.; Sun, J.; Sun, B. Experiment and methodology for drilling fluid rheology determination under high-temperature and high-pressure conditions. *Phys. Fluids* **2025**, *37*, 043108. [\[CrossRef\]](#)
- Mebarki, B.; Mohammed, K.; Imtiaz, M.; Belkacem, D.; Medal, M.; Benhanifia, K.; Jamshed, W.; Eid, M.R.; El Din, S.M. A CFD examination of free convective flow of a non-Newtonian viscoplastic fluid using ANSYS Fluent. *Arab. J. Chem.* **2023**, *16*, 105309. [\[CrossRef\]](#)
- Mishra, S.K.; Arora, A.; Kumar, Y.; Sao, M.; Chandra, H.S. CFD study of heat transfer effect on nanofluid of Newtonian and non-Newtonian type under vibration. *Chem. Prod. Process Model.* **2021**, *16*, 251–260. [\[CrossRef\]](#)
- Bandyopadhyay, T.K.; Das, S.K. Non-Newtonian and gas-non-Newtonian liquid flow through elbows: CFD analysis. *J. Appl. Fluid Mech.* **2013**, *6*, 131–141. [\[CrossRef\]](#)
- Shi, X.; Li, S.; Wei, Y.; Gao, J. Numerical investigation of laminar convective heat transfer and pressure drop of water-based Al₂O₃ nanofluids in a microchannel. *Int. Commun. Heat Mass Transf.* **2018**, *90*, 111–120. [\[CrossRef\]](#)
- Rios-Irribé, E.Y.; Cervantes-Gaxiola, M.E.; Rubio-Castro, E.; Hernandez-Calderon, O.M. Heat transfer analysis of a non-Newtonian fluid flowing through a plate heat exchanger using CFD. *Appl. Therm. Eng.* **2016**, *101*, 262–272. [\[CrossRef\]](#)
- Xiong, Q.; Bozorg, M.V.; Doranhegard, M.H.; Hong, K.; Lorenzini, G. A CFD investigation of the effect of non-Newtonian behavior of Cu-water nanofluids on their heat transfer and flow friction characteristics. *J. Therm. Anal. Calorim.* **2020**, *139*, 2601–2621. [\[CrossRef\]](#)
- Agwu, O.E.; Akpabio, J.U.; Ekpenyong, M.E.; Inyang, U.G.; Asuquo, D.E.; Eyoh, I.J.; Adeoye, O.S. A comprehensive review of laboratory, field and modelling studies on drilling mud rheology in high temperature high pressure conditions. *J. Nat. Gas Sci. Eng.* **2021**, *94*, 104046. [\[CrossRef\]](#)
- Shekhar, R.; Chakravorty, A. Investigating non-Newtonian fluid behavior in hydroswirlings via computational fluid dynamics. *Int. J. Innov. Sci. Res. Technol.* **2024**, 2331–2348. [\[CrossRef\]](#)
- Narasimha, M.; Vakamalla, T.; Kumar, M.; Mainza, A. Computational modelling of particle-fluid dynamics in comminution and classification: A review. *Miner. Process. Extr. Metall.* **2020**, *129*, 145–156. [\[CrossRef\]](#)
- Yang, L.; Tian, J.-L.; Yang, Z.; Li, Y.; Fu, C.-H.; Zhu, Y.-H.; Pang, X.-L. Numerical analysis of non-Newtonian rheology effect on hydrocyclone flow field. *Petroleum* **2015**, *1*, 68–74. [\[CrossRef\]](#)
- Liu, M.; Chen, J.; Cai, X.; Han, Y.; Xiong, S. Oil-water pre-separation with a novel axial hydroswirling. *Chin. J. Chem. Eng.* **2018**, *26*, 60–66. [\[CrossRef\]](#)
- Liu, H.; Xu, J.; Wu, Y.; Zheng, Z. Numerical study on oil and water two-phase flow in a cylindrical swirling. *J. Hydrodyn. Ser. B* **2010**, *22*, 832–837. [\[CrossRef\]](#)

21. Yang, L.; Chen, Y.; Yao, L.; Jing, F. Distribution characteristics of non-Newtonian fluid swirling flow field in a vane-type separator. *Phys. Fluids* **2023**, *35*, 103309. [[CrossRef](#)]
22. Pouraria, H.; Seo, J.K.; Paik, J.K. Numerical modelling of two-phase oil-water flow patterns in a subsea pipeline. *Ocean Eng.* **2016**, *115*, 135–148. [[CrossRef](#)]
23. Mendez, C.G.; Nigro, N.; Cardona, A. Drag and non-drag force influences in numerical simulations of metallurgical ladles. *J. Mater. Process. Technol.* **2005**, *160*, 296–305. [[CrossRef](#)]
24. de Araujo, C.A.O.; Scheid, C.M.; Loureiro, J.B.R.; Klein, T.S.; Medronho, R.A. Hydrocyclone for oil-water separations with high oil content: Comparison between CFD simulations and experimental data. *J. Pet. Sci. Eng.* **2020**, *187*, 106788. [[CrossRef](#)]
25. Huang, S. Numerical simulation of oil-water hydroswhirling using Reynolds-stress model for Eulerian multiphase flows. *Can. J. Chem. Eng.* **2005**, *83*, 829–834. [[CrossRef](#)]
26. ANSYS Inc. *Ansys Fluent User's Guide, Release 2024 R1*; ANSYS, Inc.: Canonsburg, PA, USA, 2024.
27. Alsharif, A.M.A.; Alzahrani, B.; Refaey, H.A.; Farhan, M.; Karali, M.A.; Elsayed, K. Numerical simulation of a square cyclone having different baffle configurations installed inside the inlet manifold. *J. Braz. Soc. Mech. Sci. Eng.* **2026**, *48*, 513. [[CrossRef](#)]
28. Rankine, W.J.M. *A Manual of Applied Mechanics*, 16th ed.; Charles Griffin and Company: London, UK, 1901; pp. 574–578.

Disclaimer/Publisher's Note: The statements, opinions and data contained in all publications are solely those of the individual author(s) and contributor(s) and not of MDPI and/or the editor(s). MDPI and/or the editor(s) disclaim responsibility for any injury to people or property resulting from any ideas, methods, instructions or products referred to in the content.