

Article

Numerical Investigation of Passive Flow Control over an External Backward-Facing Step Using Rigid and Elastic Plates

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Featured Application

The findings of this study provide insight into passive flow-control mechanisms in separated flow configurations represented by the backward-facing step benchmark. In particular, the results demonstrate the effectiveness of rigid cantilevered plates in reducing recirculation length and improving pressure recovery, while highlighting the influence of structural flexibility and bending stiffness on flow-control performance. This study further contributes to the understanding of fluid–structure interaction effects in separated flows and may serve as a reference for the future development of passive flow-control concepts for engineering applications involving flow separation.

Abstract

This study investigates the passive control of backward-facing step flows using rigid and elastic cantilevered plates, with emphasis on flow reattachment, pressure recovery, and fluid–structure interaction effects. The numerical methodology was first validated against an experimental benchmark, yielding a reattachment-length prediction within 1.28% of the measured value. Following validation, simulations were performed using the unsteady Reynolds-averaged Navier–Stokes equations coupled with the Spalart–Allmaras turbulence model. Rigid plate configurations were examined at momentum-thickness-based Reynolds numbers of $Re_\theta = 500$, 1000, and 5000, while two-way fluid–structure interaction simulations were conducted at $Re_\theta = 5000$ to evaluate the influence of structural stiffness. For the baseline configuration, the non-dimensional reattachment length and base drag coefficient remained within the ranges of $x_r/h = 6.18$ – 6.34 and $c_B = 0.199$ – 0.205 , respectively. The most effective rigid configuration, $L/h = 2.5$, reduced the reattachment length from $x_r/h = 6.34$ to $x_r/h = 5.57$ and the base drag coefficient from $c_B = 0.205$ to $c_B = 0.175$ at $Re_\theta = 5000$, corresponding to reductions of approximately 12% and 15%, respectively. The fluid–structure interaction simulations showed that plate stiffness strongly influences flow-control effectiveness. The stiffest elastic configuration, with $E = 2 \times 10^9$ Pa, achieved $x_r/h = 6.14$ and $c_B = 0.196$, whereas more flexible plates exhibited larger deformation and reduced aerodynamic benefit. Overall, the results demonstrate that cantilevered plates provide an effective passive flow-control strategy for backward-facing step flows. Rigid plates deliver the greatest aerodynamic improvement, while elastic plates require sufficient structural stiffness to maintain favorable pressure recovery and flow-reattachment characteristics.



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conditions of the [Creative Commons](https://creativecommons.org/licenses/by/4.0/)[Attribution \(CC BY\)](https://creativecommons.org/licenses/by/4.0/) license.**Keywords:** backward-facing step; passive flow control; fluid–structure interaction; CFD

1. Introduction

Backward-facing step (BFS) flow is a canonical configuration widely used to investigate separated flows in fluid dynamics. It provides a well-defined framework for examining key physical phenomena such as flow separation, shear-layer development, vortex dynamics, and reattachment processes. These features play a critical role in determining aerodynamic performance, structural loading, and flow-induced unsteadiness in a wide range of engineering applications. In particular, separation over step-like geometries can lead to increased drag, enhanced pressure fluctuations, and unsteady loading, and under certain conditions, similar mechanisms are associated with aerodynamic stall in lifting surfaces. Armaly et al. [1] have conducted one of the first comprehensive studies to investigate the backwards-facing step problem.

BFS-type flow structures are encountered in numerous practical systems. Representative examples include high angle of attack airfoils, internal flows in engine inlet ducts, external flows around buildings, and vehicle aerodynamics. Consequently, controlling separated flow in such configurations is essential for improving aerodynamic efficiency and ensuring reliable operation [2]. Effective flow-control strategies have been shown to reduce drag and improve energy efficiency in bluff-body flows [3,4]. In thermal-fluid systems, such as cooling channels and combustion chambers, manipulation of the recirculation region and reattachment length can enhance heat transfer and flame stabilization [5]. Similarly, mitigating vortex-induced vibrations and flow-induced structural responses requires a better understanding and control of separated flow behavior [6,7]. Flow control of bluff bodies and backwards-facing step using fluid–structure interaction has been popular in fluid mechanics research in recent years [8–10]. Nevertheless, despite substantial research activity in this area, the role of fluid–structure interaction remains insufficiently explored in both experimental and numerical studies.

It is important to position the present passive flow-control strategy within the broader context of active flow-control approaches widely investigated in the literature. The primary objective of active flow-control methods is to reduce the size of the recirculation region and shorten the reattachment length by actively manipulating the separated shear layer. To achieve this, external energy input and sophisticated actuation systems are generally required.

Table 1 summarizes representative backward-facing step flow-control studies and the corresponding reductions in reattachment length (X_r) achieved using different control techniques. Among active flow-control approaches, wall discharge and synthetic-jet-based methods demonstrated particularly strong performance. For instance, wall discharge techniques achieved approximately 68–74% reduction in reattachment length [11], while reductions of nearly 50% and 65% were reported by Xu et al. [12] and Takano et al. [13], respectively. Similarly, visual-feedback control systems [4] and inlet pulsation techniques [14] achieved reattachment-length reductions of approximately 60% and 80%, respectively. It should be noted that the Reynolds numbers reported in Table 1 are presented in their original form as reported by the respective authors and are generally based on the step height, $Re_h = U_\infty h / \nu$.

Other active flow-control methods, including plasma synthetic jet actuation [7,15,16], internal oscillators [17,18], vortex-generator jets [19], feedback control [20], and external acoustic forcing [21], also demonstrated measurable reductions in reattachment length, typically ranging between 3% and 37%.

Compared with active flow-control strategies, passive methods generally yield more modest improvements in reattachment behavior. For example, Moulinos et al. [10] reported approximately 13% reduction in reattachment length using elastic ground condition after the step. Although passive flow-control methods may not achieve the same level of

performance as highly optimized active systems, they offer important practical advantages, including simplicity, reduced implementation cost, elimination of external power requirements, and improved mechanical reliability.

Table 1. Summary of representative backward-facing step flow-control studies and corresponding reattachment-length reductions.

Author & Year	ER	Re_h	Flow-Control Type	X_r Reduction (%)
Bhattacharjee et al. (1986) [21]	1.10	27,000–76,000	External acoustic	10–15
Chun & Sung (1996) [17]	1.50	13,000–33,000	Internal oscillator	36
Rhee & Sung (2000) [18]	1.50	33,000	Internal oscillator	20
Kang & Choi (2002) [20]	1.20	5100	Feedback control	29
Morioka & Honami (2004) [19]	1.50	27,000	Vortex generator jet	20
Kiwan (2008) [11]	1.67–2.00	25,000–51,000	Wall discharge	68–74
Tihon et al. (2012) [14]	2.00	30–1800	Inlet pulsation	50–80
Gautier & Aider (2013) [4]	1.27	0–3300	Visual feedback	60
D’Adamo et al. (2014) [7]	1.14	1520	Plasma actuation	37
Takano et al. (2014) [13]	1.67	300–900	Synthetic jet	60–65
Xu et al. (2015) [12]	–	40,665	Synthetic jet	50
Correale (2015) [15]	–	3600	Plasma actuation	25
Ruisi et al. (2016) [16]	1.08	64,000	Plasma actuation	3
Moulinos et al. (2022) [10]	2.00	400–800	Elastic membrane	12.7–14.3

Although passive appendages and flexible structures have been extensively investigated in bluff-body and separated flow applications, a systematic comparison between rigid and elastic cantilevered control elements in an externally developing backward-facing step flow has not been reported. Furthermore, the role of non-dimensional bending stiffness in governing the relationship between flow reattachment and pressure recovery remains poorly understood. Since reductions in reattachment length do not necessarily imply improved aerodynamic performance, a detailed assessment of both recirculation behavior and pressure recovery is required to evaluate the effectiveness of passive flow-control devices.

The central hypothesis of this study is that the primary recirculation region downstream of the backward-facing step can be effectively manipulated by introducing a plate positioned at mid-height of the step. The plate interacts with the separated shear layer, redistributing vorticity and enhancing viscous dissipation within the recirculation zone. In the elastic configuration, the coupled fluid–structure interaction induces dynamic plate deformation, introducing additional unsteady effects and further modifying the evolution of the separated shear layer.

The proposed flow-control mechanism may be interpreted in the context of separated shear-layer dynamics and vorticity transport. In a backward-facing step flow, the separated shear layer formed downstream of the step is susceptible to Kelvin–Helmholtz instability, which promotes the roll-up of coherent vortical structures within the recirculation region. By introducing a plate within the step, the evolution of these structures is disturbed by wall-induced viscous effects and modifications to the local velocity gradients. As a result, the coherence and persistence of the recirculating vortices may be reduced, leading to attenuation of the separated flow. In the elastic plate configuration, fluid–structure interaction introduces additional unsteady deformation that may further alter the instability characteristics of the shear layer and influence the pressure recovery process.

In this study, a passive flow-control strategy for backward-facing step flow is investigated using rigid and elastic cantilevered plates attached to the step edge. A validated numerical framework is employed to examine the influence of plate length, Reynolds number, and structural stiffness on the reattachment length, base drag coefficient, and transient flow response. For the elastic plate cases, a moving-mesh technique combined with two-way fluid–structure interaction coupling is employed to resolve the mutual interaction between the deforming structure and the separated flow field. The remainder of this paper

is organized as follows. Section 2 outlines the physical problem, numerical methodology, and validation procedure. Section 3 presents and discusses the results of the proposed passive flow-control strategy. Finally, the main conclusions are summarized in Section 4.

2. Problem Description and Methodology

Unlike the canonical backward-facing step problem typically studied in confined channel flows, the present configuration features a relatively large expansion ratio and a channel height, thereby minimizing blockage and confinement effects. As a result, the flow field approaches the behavior of an external separated flow, characterized by boundary-layer separation at the step edge and the subsequent formation of a free shear layer spanning the recirculation region. This makes the problem more representative of practical aerodynamic flow separation phenomena. The benchmark configuration of Driver and Seegmiller [22] employs a step height of $h = 0.5$ in (12.7 mm) and a step-height-to-channel-height ratio of 1:9, thereby minimizing the influence of the streamwise pressure gradient associated with the sudden expansion. Furthermore, a step-height-to-channel-width ratio of 1:12 is used to suppress sidewall effects and promote two-dimensional flow development within the test section.

Figure 1 presents the geometry considered in the present study.

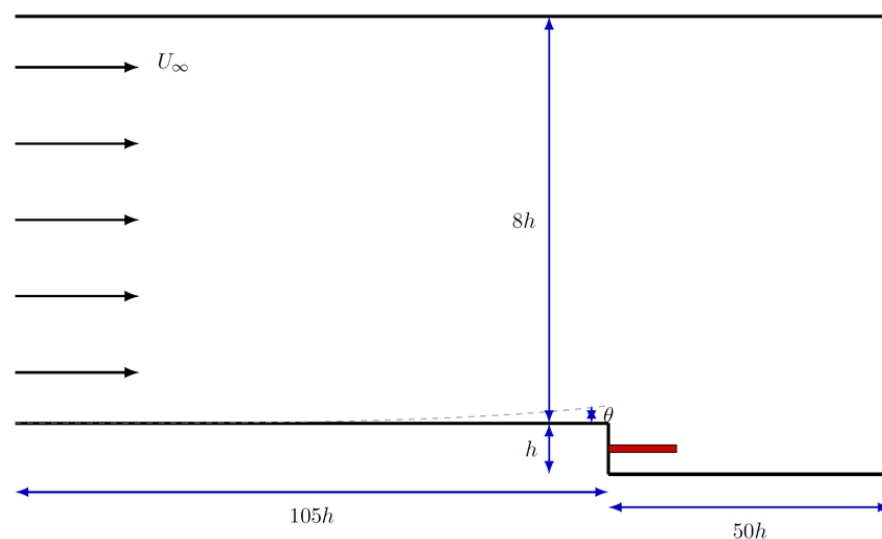


Figure 1. Schematic of the backward-facing step configuration investigated in the present study.

Table 2 presents the definitions of key dimensionless parameters utilized in the study.

Table 2. Definition and key dimensionless parameters utilized in the present study.

Reynolds number	$Re_\theta = \frac{U_\infty \theta}{\nu}$
Bending stiffness	$k^* = \frac{EI}{\rho U_\infty^2 h^3}$
Pressure coefficient	$C_p = \frac{p - p_{ref}}{\frac{1}{2} \rho U_{ref}^2}$
Base drag coefficient	$C_B = \frac{-1}{h} \int_0^h C_p(y) dy$
Wall shear stress coefficient	$C_f = \frac{\tau_w}{\frac{1}{2} \rho U_\infty^2}$

2.1. Theoretical Background and Equations

The governing flow equations are solved within the framework of the incompressible unsteady Reynolds-averaged Navier–Stokes (URANS) approach. Turbulence closure is achieved using the Spalart–Allmaras model, which was selected following a turbulence-model sensitivity study against the benchmark experiments of Driver and Seegmiller [22]. Although the SST k – ω model yielded the smallest error in predicting reattachment length, the Spalart–Allmaras model provided comparable accuracy while offering improved numerical robustness and a significantly lower computational cost. This consideration becomes particularly important for the two-way coupled fluid–structure interaction simulations performed in the present study.

The primary objective of the present work is to evaluate the relative influence of rigid and elastic cantilevered plates on separated flow behavior. Consequently, the numerical methodology is required to accurately reproduce the separated shear-layer development, recirculation dynamics, and flow-reattachment process rather than resolve detailed near-wall turbulence structures. Under these conditions, the Spalart–Allmaras model provides an appropriate balance between accuracy and computational efficiency.

Performance Metrics

The incoming turbulent boundary layer immediately upstream of the backward-facing step is characterized by the Reynolds number based on the momentum thickness, defined as

$$Re_\theta = \frac{U_\infty \theta}{\nu}, \quad (1)$$

where (U_∞) is the reference freestream velocity, (θ) is the momentum thickness evaluated at the step edge immediately upstream of the separation point, and (ν) is the kinematic viscosity of the fluid.

The momentum thickness is calculated from the boundary-layer velocity profile according to

$$\theta = \int_0^\delta \frac{U}{U_\infty} \left(1 - \frac{U}{U_\infty}\right) dy, \quad (2)$$

where (U) is the local streamwise velocity, (U_∞) is the free-stream velocity, and (δ) is the boundary-layer thickness. Physically, the momentum thickness represents the momentum deficit generated by viscous effects within the boundary layer. In the present study, (θ) is evaluated immediately upstream of the step edge to characterize the incoming boundary layer prior to flow separation.

The base drag coefficient is determined from the pressure distribution along the vertical face of the backward-facing step. It is defined as

$$C_B = -\frac{1}{h} \int_0^h C_p(y) dy, \quad (3)$$

where (h) is the step height and (C_p) is the pressure coefficient along the step face. The pressure coefficient is given by

$$C_p = \frac{p - p_{\text{ref}}}{\frac{1}{2} \rho U_{\text{ref}}^2}, \quad (4)$$

where (p) is the local static pressure, (p_{ref}) is the reference pressure, (ρ) is the fluid density, and (U_{ref}) is the reference freestream velocity.

The negative sign in Equation (3) accounts for the orientation of the pressure force relative to the freestream direction. A lower pressure within the recirculation region, therefore, corresponds to a larger positive value of (C_B), indicating a stronger contribution of the separated flow to the overall pressure drag.

The reattachment length, x_r , is defined as the streamwise distance between the step edge and the location where the time-averaged wall shear stress changes sign from negative to positive. In the present study, the reattachment length is reported in non-dimensional form as x_r/h , where h is the step height.

2.2. Governing Equations and Numerical Modeling

The numerical simulations were performed using COMSOL Multiphysics (version 6.2) and consisted of three coupled components: the fluid flow solver, the structural mechanics solver, and the fluid–structure interaction (FSI) framework. The fluid domain was modeled using the incompressible unsteady Reynolds-averaged Navier–Stokes (URANS) equations coupled with the Spalart–Allmaras turbulence model, while the structural response of the elastic cantilevered plate was described using a geometrically nonlinear linear-elastic formulation [23]. For the FSI cases, deformation of the plate was transferred to the surrounding fluid domain through an Arbitrary Lagrangian–Eulerian (ALE) moving-mesh approach, allowing the mutual interaction between the separated flow and the deformable structure to be captured throughout the transient simulation.

2.2.1. Fluid Flow and Turbulence Modeling

The fluid flow was modeled using the incompressible unsteady Reynolds-averaged Navier–Stokes (URANS) equations. The governing continuity and momentum equations are

$$\frac{\partial u_i}{\partial x_i} = 0 \quad (5)$$

$$\rho \left(\frac{\partial u_i}{\partial t} + u_j \frac{\partial u_i}{\partial x_j} \right) = -\frac{\partial p}{\partial x_i} + \frac{\partial}{\partial x_j} \left[(\mu + \mu_t) \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) \right]. \quad (6)$$

Turbulence closure was achieved using the one-equation Spalart–Allmaras turbulence model [24,25]. The model solves a transport equation for a modified turbulent kinematic viscosity and has been extensively applied to external aerodynamic flows involving adverse pressure gradients, separation, and reattachment. In the present study, the model was selected based on the turbulence-model sensitivity analysis presented in Section 2.2.5, where it demonstrated good agreement with the benchmark data while requiring substantially lower computational effort than the SST $k-\omega$ model.

$$\begin{aligned} \frac{\partial \tilde{\nu}}{\partial t} + u_j \frac{\partial \tilde{\nu}}{\partial x_j} = & C_{b1}(1 - f_{t2})\tilde{S}\tilde{\nu} \\ & + \frac{1}{\sigma} \left[\frac{\partial}{\partial x_j} \left((\nu + \tilde{\nu}) \frac{\partial \tilde{\nu}}{\partial x_j} \right) + C_{b2} \left(\frac{\partial \tilde{\nu}}{\partial x_j} \right)^2 \right] \\ & - \left[C_{w1}f_w - \frac{C_{b1}}{\kappa^2}f_{t2} \right] \left(\frac{\tilde{\nu}}{d} \right)^2 + f_{t1}\Delta U^2. \end{aligned} \quad (7)$$

where the turbulent eddy viscosity is evaluated as

$$\nu_t = \tilde{\nu}f_{v1}, \quad (8)$$

with

$$f_{v1} = \frac{\chi^3}{\chi^3 + C_{v1}^3}, \quad \chi = \frac{\tilde{\nu}}{\nu}. \quad (9)$$

No external body force was included in the governing equations. Flow control was achieved solely through the introduction of rigid or elastic cantilevered plates.

2.2.2. Structural Modeling

For the fluid–structure interaction simulations, the cantilevered plate was modeled as a linearly elastic solid. The structural momentum balance in the material reference configuration is expressed as

$$\rho_{s,0} \frac{\partial^2 \mathbf{d}_s}{\partial t^2} = \nabla_{\mathbf{X}} \cdot \mathbf{P}_s + \rho_{s,0} \mathbf{b}_s, \quad (10)$$

where $\rho_{s,0}$ is the reference density of the solid, $\mathbf{d}_s$ is the displacement vector, $\mathbf{P}_s$ is the first Piola–Kirchhoff stress tensor, and $\mathbf{b}_s$ denotes the body force per unit mass.

The deformation gradient tensor and Green–Lagrange strain tensor are defined as

$$\mathbf{F}_s = \mathbf{I} + \nabla_{\mathbf{X}} \mathbf{d}_s, \quad (11)$$

$$\mathbf{E}_s = \frac{1}{2} (\mathbf{F}_s^T \mathbf{F}_s - \mathbf{I}). \quad (12)$$

Assuming linear elasticity, the constitutive relation becomes

$$\mathbf{S}_s = \mathbf{C} : \mathbf{E}_s, \quad (13)$$

where $\mathbf{S}_s$ is the second Piola–Kirchhoff stress tensor and $\mathbf{C}$ is the elasticity tensor. The corresponding first Piola–Kirchhoff stress tensor is

$$\mathbf{P}_s = \mathbf{F}_s \mathbf{S}_s. \quad (14)$$

The structural domain was modeled using a two-dimensional plane-strain formulation. Geometric nonlinearity was retained to account for finite displacements and rotations arising from fluid loading.

2.2.3. ALE Moving-Mesh Formulation

The deformation of the elastic plate modifies the geometry of the surrounding fluid domain. Therefore, an Arbitrary Lagrangian–Eulerian (ALE) moving-mesh formulation was employed [26]. The mapping between the reference and deformed fluid domains is given by

$$\mathbf{x} = \mathbf{X} + \mathbf{d}_m, \quad (15)$$

where $\mathbf{d}_m$ denotes the mesh displacement. The corresponding mesh velocity is

$$\mathbf{w}_m = \frac{\partial \mathbf{d}_m}{\partial t}. \quad (16)$$

Using the ALE description, the momentum equation is modified through the relative convective velocity ($\mathbf{u}_f - \mathbf{w}_m$):

$$\rho_f \left[\frac{\partial \mathbf{u}_f}{\partial t} + (\mathbf{u}_f - \mathbf{w}_m) \cdot \nabla \mathbf{u}_f \right] = -\nabla p + \nabla \cdot \left[(\mu_f + \mu_t) (\nabla \mathbf{u}_f + \nabla \mathbf{u}_f^T) \right]. \quad (17)$$

The incompressibility condition remains

$$\nabla \cdot \mathbf{u}_f = 0. \quad (18)$$

To preserve mesh quality during transient deformation, the internal mesh nodes were redistributed using hyperelastic mesh smoothing. This artificial constitutive law

is employed solely for mesh motion and is independent of the physical material model assigned to the cantilevered plate.

The transient fluid–structure interaction simulations were performed using a fixed time step of $\Delta t = 10^{-5}$ s. At each time step, the fluid, turbulence, structural, and moving-mesh equations were solved in a coupled iterative manner until convergence was achieved. Convergence was assessed by monitoring the principal flow quantities, including the reattachment length and base drag coefficient. A statistically converged solution was assumed when the variation in these quantities remained below 0.01% over the previous 100 time steps, corresponding to a solution repeatability greater than 99.99%. The selected time step was found to provide stable and temporally independent predictions for all configurations considered in the present study.

2.2.4. Fluid–Structure Interaction Coupling

The interaction between the turbulent flow and the elastic cantilevered plate was treated as a fully coupled two-way fluid–structure interaction problem. At the fluid–solid interface, displacement compatibility was enforced according to

$$\mathbf{d}_m = \mathbf{d}_s \quad \text{on } \Gamma_{\text{FSI}}, \quad (19)$$

while the moving-wall velocity condition was imposed as

$$\mathbf{u}_f = \frac{\partial \mathbf{d}_s}{\partial t} \quad \text{on } \Gamma_{\text{FSI}}. \quad (20)$$

Dynamic equilibrium at the interface was enforced through traction continuity:

$$\boldsymbol{\sigma}_f \mathbf{n}_f + \boldsymbol{\sigma}_s \mathbf{n}_s = \mathbf{0} \quad \text{on } \Gamma_{\text{FSI}}. \quad (21)$$

At stationary boundaries of the moving-mesh domain, the mesh displacement was prescribed as

$$\mathbf{d}_m = \mathbf{0} \quad \text{on } \Gamma_{m,0}. \quad (22)$$

For the rigid plate configurations, the plate was treated as a stationary no-slip wall and no fluid–structure interaction was considered. The coupled fluid, turbulence, structural, and ALE mesh equations were solved in a transient framework using a segregated solution strategy.

2.2.5. Mesh Independence Study and Experimental Validation

Prior to the validation study, a mesh independence analysis was conducted using the Driver and Seegmiller [22] benchmark configuration. Five successively refined computational grids were generated, ranging from 19,855 to 109,905 elements. The non-dimensional reattachment length, x_r/h , was selected as the primary convergence metric due to its sensitivity to the prediction of flow separation and reattachment.

As shown in Table 3, the predicted reattachment length gradually converges toward the experimental value as the mesh is refined. The reattachment length decreases from $x_r/h = 6.47$ on the coarsest mesh to $x_r/h = 6.28$ on the finest mesh, corresponding to a reduction in error from 3.35% to only 0.28%. Furthermore, the near-wall mesh resolution satisfies the requirements of low-Reynolds-number turbulence modeling, with y^+ values close to unity for all grids.

Although Grid 5 provides the highest accuracy, the improvement relative to Grid 4 is modest. The predicted reattachment length changes from $x_r/h = 6.34$ to $x_r/h = 6.28$, corresponding to a difference of less than 1%, while the total number of elements increases by

approximately 91%. Consequently, Grid 4 was selected for all subsequent simulations, as it provides an appropriate balance between computational efficiency and numerical accuracy.

Table 3. Mesh independence study for the baseline backward-facing step configuration.

Grid	Number of Elements	Average Element Quality	y^+	x_r/h	Error (%)
1	19,855	0.9216	1.46	6.47	3.35
2	30,482	0.9308	1.34	6.38	1.92
3	43,359	0.9346	1.22	6.37	1.76
4	57,611	0.9394	1.02	6.34	1.28
5	109,905	0.9691	1.00	6.28	0.28

Prior to selecting the turbulence model for the present study, a turbulence-model sensitivity analysis was performed using the Driver and Seegmiller benchmark configuration. Four commonly used turbulence models, namely the standard $k-\epsilon$, standard $k-\omega$, SST $k-\omega$, and Spalart–Allmaras models, were evaluated based on their prediction of the non-dimensional reattachment length. The corresponding results are summarized in Table 4.

As shown in Table 4, the SST $k-\omega$ model provided the closest agreement with the experimental measurements, predicting a reattachment length of $x_r/h = 6.28$, corresponding to an error of only 0.32%. The Spalart–Allmaras model also demonstrated good agreement, yielding a reattachment length of $x_r/h = 6.34$ with an error of 1.28%. In contrast, the standard $k-\epsilon$ and standard $k-\omega$ models produced significantly larger deviations from the experimental data.

Considering the improved numerical robustness and substantially lower computational cost of the Spalart–Allmaras model, particularly for the transient two-way fluid–structure interaction simulations performed in the present study, the Spalart–Allmaras model was selected for all subsequent computations.

Following turbulence-model selection, a detailed validation was performed using the Spalart–Allmaras model. The predicted reattachment length was $x_r/h = 6.34$, corresponding to an error of 1.28% relative to the experimental measurements. The close agreement with the benchmark data demonstrates that the model accurately captures the dominant separated flow features, including shear-layer development, recirculation, and flow reattachment, while maintaining an efficient computational framework for the extensive parametric study conducted herein.

Table 4. Comparison of turbulence models for the Driver and Seegmiller [22] backward-facing step benchmark.

Turbulence Model	x_r/h	Error (%)
Standard $k-\epsilon$	5.36	14.38
Standard $k-\omega$	7.57	20.93
SST $k-\omega$	6.28	0.32
Spalart–Allmaras	6.34	1.28
Experiment [22]	6.26	–

Therefore, the Spalart–Allmaras model was adopted for the production simulations involving the baseline, rigid plate, and flexible plate configurations. The validation results indicate that this model is capable of capturing the dominant separated flow features, in-

cluding shear-layer development, recirculation, and reattachment, with sufficient accuracy for the comparative flow-control analysis performed in the present study.

Following turbulence-model selection, a detailed validation was conducted using the Spalart–Allmaras model and the experimental measurements of Driver and Seegmiller [22]. The validation focused on two key quantities governing separated flow behavior, namely the non-dimensional reattachment length, x_r/h , and the pressure coefficient distribution. Table 5 presents the comparison between the numerical and experimental results.

The SST k – ω model predicted the reattachment length with an error of only 0.32%, demonstrating excellent agreement with the experimental benchmark. The pressure coefficient prediction also showed good agreement, with a deviation of 7.28%. These results confirm that the selected numerical methodology accurately reproduces the primary flow features associated with turbulent backward-facing step flows, including shear-layer development, recirculation dynamics, and flow reattachment.

Furthermore, the obtained results are consistent with previous validation studies employing two-equation turbulence models for the Driver and Seegmiller benchmark configuration [27,28]. Therefore, the numerical methodology is considered sufficiently accurate for investigating the effects of rigid and flexible cantilevered plates on the flow characteristics of the backward-facing step.

Table 5. Comparison of the present numerical predictions with the experimental data of Driver and Seegmiller [22].

Parameter	Experiment	Present CFD	Error (%)
x_r/h	6.26	6.34	1.28
C_p	0.12195	0.12198	0.02

Figure 2 compares the pressure coefficient distribution obtained from the present numerical simulation with the experimental measurements of Driver and Seegmiller [22]. Immediately downstream of the step, both datasets exhibit a region of low pressure associated with the separated recirculation zone. As the flow develops downstream, a gradual pressure recovery is observed due to momentum exchange within the separated shear layer and the eventual reattachment of the flow to the lower wall.

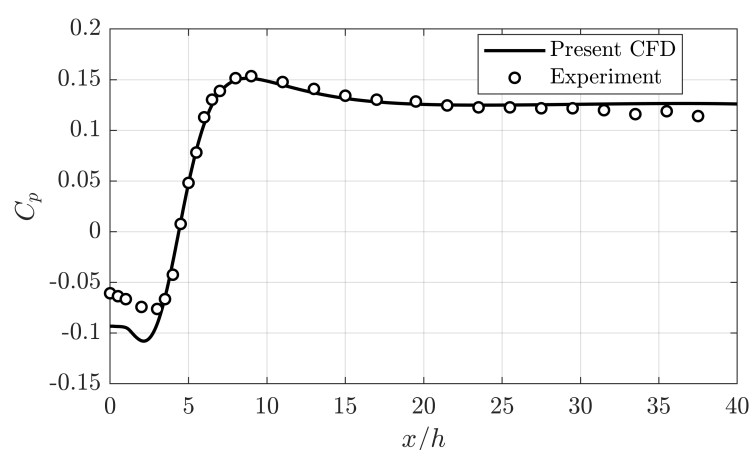


Figure 2. Comparison of the streamwise pressure coefficient distribution predicted by the present CFD model with the experimental measurements of Driver and Seegmiller [22].

The present CFD model successfully reproduces the overall pressure recovery trend and accurately captures the location and magnitude of the pressure rise through the reattachment region. Furthermore, the downstream pressure distribution shows good agreement

with the experimental measurements, indicating that the selected turbulence model is capable of predicting the dominant flow physics associated with turbulent backward-facing step flows. The close agreement between the numerical and experimental pressure distributions provides additional confidence in the accuracy of the present computational methodology.

Figure 3 presents the comparison between the wall skin-friction coefficient distribution predicted by the present CFD model and the experimental measurements of Driver and Seegmiller [22]. Immediately downstream of the step, negative values of C_f indicate the presence of the recirculation region generated by flow separation. The skin-friction coefficient reaches a minimum value near $x/h \approx 4$ before gradually increasing and crossing the zero line at the reattachment location.

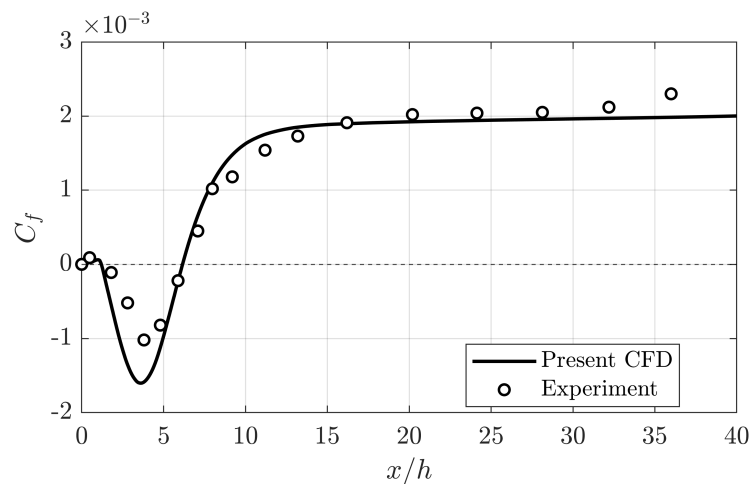


Figure 3. Comparison of the wall skin-friction coefficient distribution predicted by the present CFD model with the experimental measurements of Driver and Seegmiller [22].

The present numerical results accurately reproduce both the magnitude and spatial evolution of the experimental skin-friction coefficient distribution. In particular, the predicted zero-crossing location corresponds closely to the experimentally observed reattachment point. Downstream of reattachment, the recovery of the wall shear stress and the asymptotic approach toward the fully redeveloped turbulent boundary layer are also captured with good agreement. The excellent correspondence between the numerical and experimental data demonstrates the capability of the adopted computational methodology to predict the near-wall flow physics governing separation and reattachment in turbulent backward-facing step flows.

2.3. Passive Flow Control

The baseline configuration, without a cantilevered plate, is first analyzed to establish a reference case for comparison. A cantilevered plate with a thickness of $t = 2.59 \times 10^{-4}$ m, corresponding to a non-dimensional thickness of $t/h = 0.0204$, is introduced within the step region to modify the separated flow. Three plate lengths are considered, corresponding to $L/h = 1.0, 2.0$, and 2.5 . The plate is clamped at the step edge and remains free at its downstream end.

The passive flow-control investigation is divided into two stages. First, rigid plate simulations are performed for all Reynolds numbers considered in the study ($Re_\theta = 500, 1000$, and 5000) to assess the influence of plate length on the separated flow. Subsequently, fluid–structure interaction (FSI) simulations are conducted exclusively at $Re_\theta = 5000$.

The selection of $Re_\theta = 5000$ for the FSI investigation is motivated by several factors. First, this Reynolds number corresponds to the strongest fluid loading considered in the present study, thereby maximizing the potential structural deformation of the cantilevered

plate. Second, the baseline flow at $Re_\theta = 5000$ was validated against the benchmark experimental data of Driver and Seegmiller [22], providing confidence in the numerical predictions. Finally, restricting the FSI simulations to a single Reynolds number allows the influence of structural properties to be isolated from Reynolds-number effects, enabling a systematic investigation of plate flexibility.

For the FSI cases, the plate length is fixed at $L/h = 2.5$, corresponding to the most effective rigid plate configuration identified in the preceding analysis. A material-property sweep is then performed by varying Young's modulus while maintaining the same geometric configuration. This approach enables the influence of structural stiffness on plate deformation, flow reattachment, and pressure recovery to be examined independently of geometric effects.

The structural resistance of the elastic plate is characterized by the non-dimensional bending stiffness, k^* , which represents the ratio of structural rigidity to fluid inertial forces. Lower values of k^* correspond to more flexible plates and stronger fluid–structure interaction effects, whereas higher values indicate increasingly rigid structural behavior.

To investigate the influence of structural flexibility, Young's modulus of the cantilevered plate was varied from 2×10^8 Pa to 2×10^9 Pa while maintaining identical geometric dimensions. This resulted in a range of non-dimensional bending stiffness values between $k^* = 0.05892$ and $k^* = 0.58916$, covering both highly compliant and relatively stiff structural responses. Consequently, the selected parameter space enables the transition from strongly coupled fluid–structure interaction behavior to a near-rigid plate response to be systematically examined.

For a fixed plate geometry and flow condition, the non-dimensional bending stiffness increases linearly with Young's modulus. Therefore, increasing k^* progressively suppresses structural deformation and causes the elastic plate to behave more similarly to a rigid cantilever.

3. Results and Discussion

This section presents the results for the baseline case, rigid plate cases and elastic plate cases. All reported quantitative results, including the reattachment length and base drag coefficient, are obtained as time-averaged values over a statistically steady interval after the initial transient phase. The contour plots, however, represent instantaneous flow fields at a selected time instance.

3.1. Baseline Flow Characteristics

The baseline flow characteristics are summarized in Table 6, where the non-dimensional reattachment length, base drag coefficient, and momentum thickness immediately upstream of the step are presented for the three Reynolds numbers considered.

Table 6. Baseline flow characteristics.

Re_θ	x_r/h	c_B	θ (mm)
500	6.27	0.200	2.393
1000	6.18	0.199	2.162
5000	6.34	0.205	1.693

The results indicate that the non-dimensional reattachment length remains nearly constant throughout the investigated Reynolds number range. The predicted values vary only between ($x_r/h = 6.18$) and ($x_r/h = 6.34$), suggesting that the overall size of the recirculation region is relatively insensitive to the Reynolds number for the present geometry

and flow conditions. The largest reattachment length is observed at ($Re = 5000$), although the difference relative to the lower Reynolds number cases remains below (3%).

Similarly, the base drag coefficient exhibits only minor variations, remaining close to ($c_B \approx 0.20$) for all cases. This observation indicates that the mean pressure deficit within the recirculation region is largely unaffected by the Reynolds number over the investigated range. The nearly constant values of both (x_r/h) and (c_B) suggest that the separated flow topology remains qualitatively similar despite the increase in the Reynolds number.

The momentum thickness evaluated immediately upstream of the step decreases from ($\theta = 2.393$ mm) at ($Re = 500$) to ($\theta = 1.693$ mm) at ($Re = 5000$). This trend reflects the expected thinning of the upstream boundary layer with an increasing Reynolds number. Nevertheless, the reduction in boundary-layer thickness does not significantly alter the mean reattachment behavior, indicating that the separated shear-layer development is primarily governed by the geometric expansion associated with the backward-facing step.

These baseline results establish a consistent reference for assessing the influence of the rigid and flexible cantilevered plates introduced in the subsequent sections.

The velocity contour shown in Figure 4a illustrates the flow structure of the baseline backward-facing step configuration at $Re_\theta = 5000$. Immediately downstream of the step, the boundary layer separates and forms a large recirculation region adjacent to the lower wall. The separated shear layer originating from the step corner encloses this low-momentum region and gradually develops downstream. A small secondary corner vortex is also visible immediately behind the step, while the primary recirculation bubble occupies most of the separated flow region.

As the flow progresses downstream, momentum exchange between the high-speed outer flow and the recirculating fluid causes the shear layer to move toward the wall. This process governs the flow-reattachment mechanism and results in a non-dimensional reattachment length of $x_r/h = 6.34$. Outside the separated region, the velocity field remains relatively uniform, indicating that the dominant flow modifications are confined to the near-wall region downstream of the step.

The pressure contour presented in Figure 4b reveals a pronounced low-pressure region immediately downstream of the step. The minimum pressure occurs within the primary recirculation bubble, where the flow experiences substantial momentum loss and reverse motion. This pressure deficit extends throughout the separated flow region and gradually recovers as the shear layer approaches the wall and the flow reattaches. The low-pressure zone downstream of the step is directly responsible for the base drag generated by the baseline configuration.

The vorticity contour shown in Figure 4c highlights the separated shear layer as the dominant vortical structure governing the flow development. Strong negative vorticity is generated at the step edge due to the large velocity gradient between the outer flow and the recirculating region. The vorticity layer remains coherent over a significant downstream distance before gradually diffusing as momentum is transferred across the shear layer. The absence of large secondary vortical structures indicates that the reattachment process is primarily controlled by the downstream evolution of a single separated shear layer.

Overall, the baseline flow is characterized by a dominant recirculation bubble, a substantial pressure deficit, and a well-defined separated shear layer. These features are spatially consistent across the velocity, pressure, and vorticity fields and establish the reference flow structure against which the rigid and elastic plate configurations are evaluated. Since the objective of the proposed passive flow-control strategy is to promote earlier flow reattachment and improve pressure recovery, an effective control configuration should reduce the extent of the recirculation region while simultaneously weakening the pressure deficit downstream of the step.

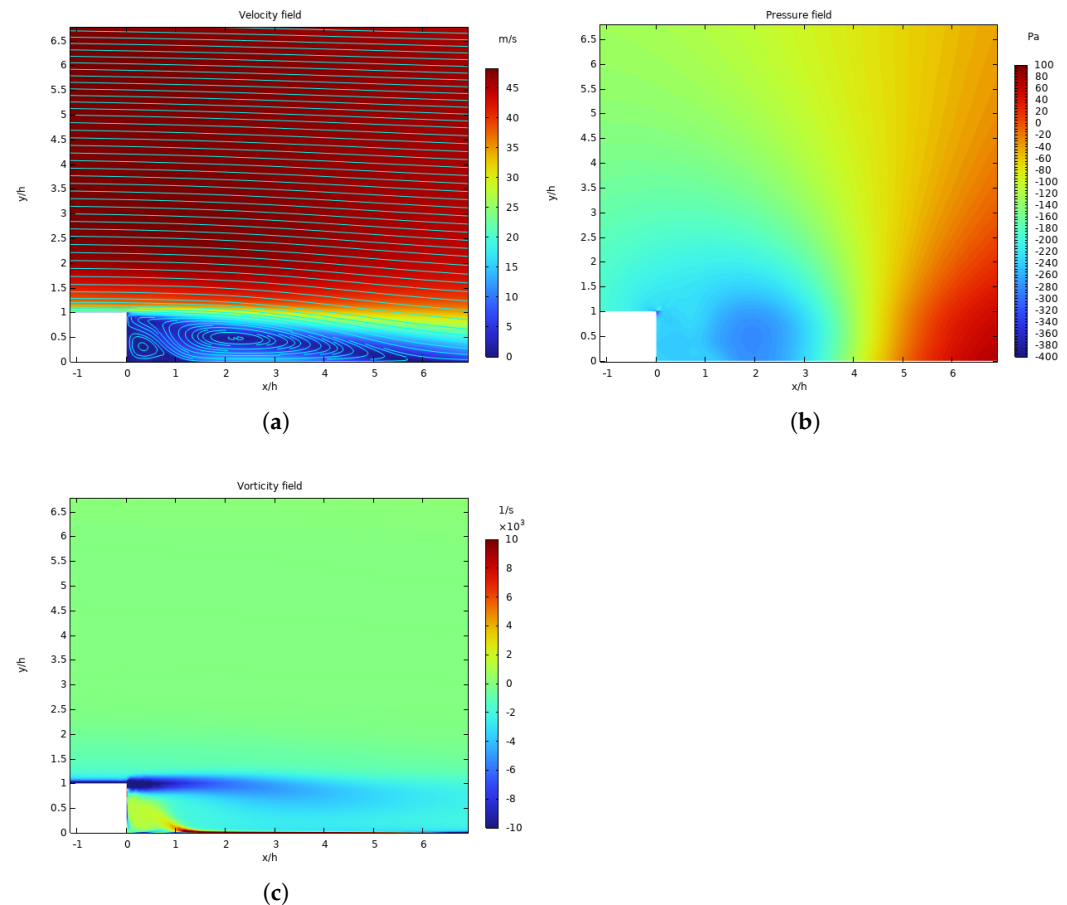


Figure 4. Baseline flow field at $Re_\theta = 5000$: (a) velocity contour, (b) pressure contour, and (c) vorticity contour.

3.2. Rigid Plate Configurations

The effect of rigid cantilevered plates on the separated flow is summarized in Tables 7 and 8, where the reattachment length and base drag coefficient are presented for different Reynolds numbers and plate lengths.

Table 7. Reattachment length (x_r/h) for rigid plate configurations.

Re_θ	$L/h = 1.0$	$L/h = 2.0$	$L/h = 2.5$
500	6.40	5.84	5.63
1000	6.36	5.69	5.52
5000	6.50	5.85	5.57

The results indicate that the effectiveness of the rigid plate is primarily governed by plate length. The shortest plate ($L/h = 1.0$) produces only minor changes in the reattachment length relative to the baseline cases, with values remaining close to ($x_r/h \approx 6.3$ – 6.5). In contrast, increasing the plate length results in a systematic reduction in the recirculation region.

For all Reynolds numbers considered, the longest plate ($L/h = 2.5$) produces the shortest reattachment length. At ($Re = 500$), the reattachment length decreases from the baseline value of ($x_r/h = 6.27$) to ($x_r/h = 5.63$), corresponding to a reduction of approximately (10.2%). Similar reductions are observed at ($Re = 1000$) and ($Re = 5000$), where the reattachment length decreases by approximately (10.7%) and (12.1%), respectively. These results demonstrate that the rigid plate effectively modifies the separated shear

layer and promotes earlier flow reattachment when the plate extends sufficiently into the recirculation region.

Table 8. Base drag coefficient (c_B) for rigid plate configurations.

Re_θ	$L/h = 1.0$	$L/h = 2.0$	$L/h = 2.5$
500	0.198	0.169	0.157
1000	0.202	0.176	0.165
5000	0.209	0.188	0.175

A similar trend is observed for the base drag coefficient. For all Reynolds numbers, increasing the plate length results in a progressive reduction in (c_B), indicating improved pressure recovery within the recirculation region. The largest improvement is again obtained with the longest plate ($L/h = 2.5$). Relative to the baseline configuration, the base drag coefficient decreases by approximately (21.5%), (17.1%), and (14.6%) for ($Re = 500$), (1000), and (5000), respectively.

The simultaneous reduction in reattachment length and base drag coefficient confirms that the rigid plate beneficially alters the separated flow structure. The plate acts as a passive guide surface within the recirculation region, reducing the extent of the separated zone while also weakening the pressure deficit downstream of the step. Among the investigated configurations, the longest plate ($L/h = 2.5$) consistently provides the best overall aerodynamic performance and is therefore selected as the reference geometry for the subsequent fluid–structure interaction study.

The velocity contour shown in Figure 5a illustrates the flow structure obtained with the rigid cantilevered plate at $Re_\theta = 5000$ and $L/h = 2.5$. Compared to the baseline configuration, the separated flow undergoes a substantial reorganization. The rigid plate intercepts the separated shear layer and forces it toward the lower wall, resulting in significantly earlier flow reattachment.

A large recirculation region is formed beneath the plate, while a secondary recirculation bubble develops downstream of the plate trailing edge. Consequently, the original recirculation region observed in the baseline flow is redistributed into smaller and more confined vortical structures. The separated shear layer follows the lower surface of the plate and reattaches near the plate trailing edge before developing a weaker downstream recirculation region. As a result, the overall extent of the separated flow is reduced, which is consistent with the reduction in reattachment length from $x_r/h = 6.34$ for the baseline case to $x_r/h = 5.57$ for the rigid plate configuration.

The pressure contour presented in Figure 5b reveals a substantial modification in the pressure field due to the presence of the rigid plate. Although a low-pressure region remains beneath the plate, its spatial extent is considerably smaller than that observed in the baseline flow. Furthermore, a noticeable pressure recovery occurs immediately downstream of the plate trailing edge, where the flow reattaches to the wall.

The increased pressure levels downstream of the plate indicate a reduction in the pressure deficit associated with the separated flow. This pressure recovery directly contributes to the reduction in the base drag coefficient from $c_B = 0.205$ for the baseline configuration to $c_B = 0.175$ for the rigid plate case.

The vorticity contour shown in Figure 5c further highlights the interaction between the separated shear layer and the rigid plate. A concentrated vorticity layer originates at the step corner and follows the lower surface of the plate. The strongest vorticity magnitudes occur near the plate leading and trailing edges, where substantial velocity gradients are generated.

Downstream of the plate, the vorticity layer rapidly weakens compared with the baseline configuration, indicating enhanced momentum exchange and accelerated dissipation of the separated shear layer. The rigid plate therefore acts as a passive flow-control device that redirects the separated shear layer toward the wall, promotes earlier reattachment, and improves pressure recovery downstream of the step.

Overall, the rigid plate substantially alters the wake structure by reducing the spatial extent of the separated flow and weakening the associated pressure deficit. These modifications lead to simultaneous reductions in both the reattachment length and base drag coefficient, demonstrating the effectiveness of the proposed passive flow-control strategy.

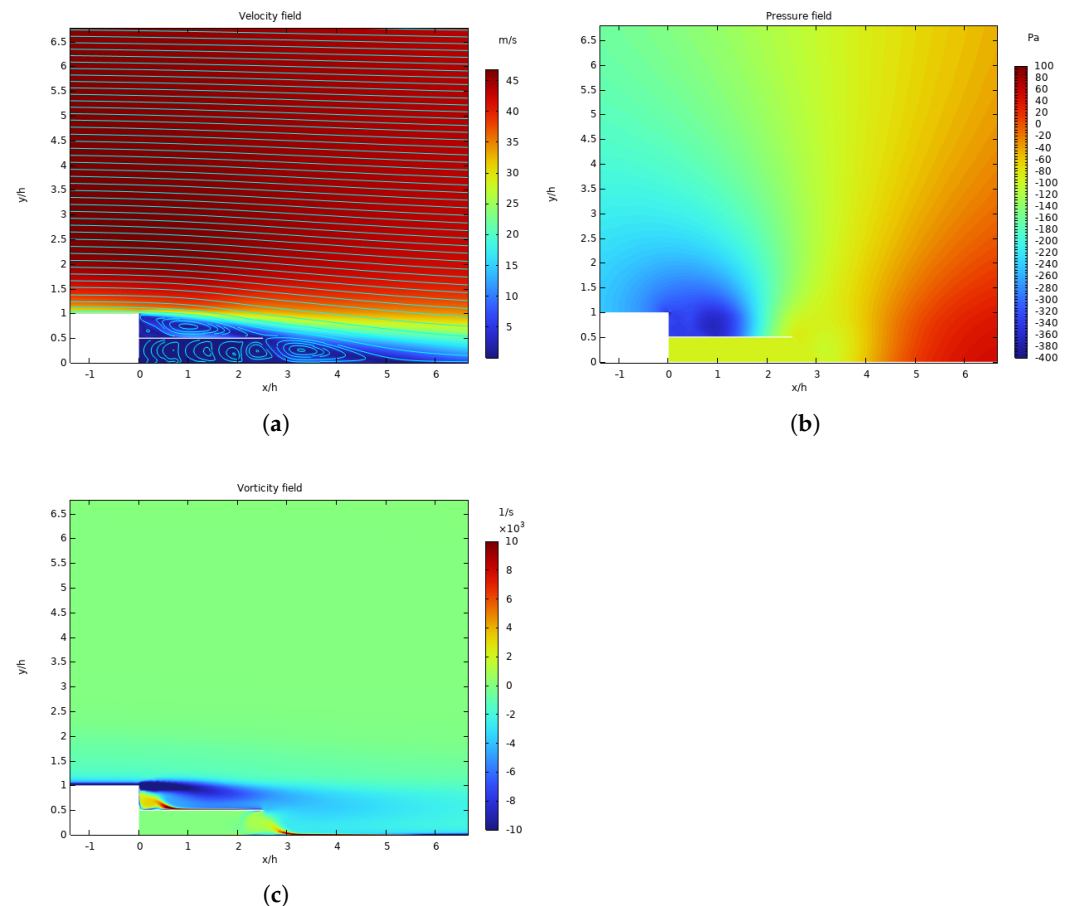


Figure 5. Flow-field contours for the rigid cantilevered plate configuration at $Re_\theta = 5000$ and $L/h = 2.5$: (a) velocity magnitude, (b) pressure field, and (c) vorticity field.

Compared to the baseline configuration, the rigid cantilevered plate substantially modifies the development of the separated shear layer downstream of the step. Rather than allowing the shear layer to expand freely into a large recirculation bubble, the plate redirects the separated flow toward the lower wall and promotes earlier reattachment. As a result, the original recirculation region is redistributed into smaller and more localized vortical structures, leading to a significant reduction in the overall separation extent.

The effectiveness of the passive flow-control strategy increases with plate length, indicating that sufficient penetration of the plate into the recirculation region is necessary to alter the shear-layer trajectory and enhance momentum exchange. The resulting flow topology exhibits a more compact separated flow region together with improved pressure recovery downstream of the plate. Consequently, simultaneous reductions in both the non-dimensional reattachment length and the base drag coefficient are achieved.

These findings demonstrate that rigid cantilevered plates can provide an effective passive flow-control mechanism for backward-facing step flows by manipulating the separated shear layer and improving the pressure distribution without requiring any external energy input.

3.3. Elastic Plate Configurations

Following the rigid plate investigation, two-way fluid–structure interaction (FSI) simulations were performed at $Re_\theta = 5000$ to examine the influence of structural flexibility on the flow-control performance. The elastic plates were modeled using a moving-mesh approach with two-way coupling between the fluid and structural solvers.

To first assess the influence of plate length under elastic conditions, a representative Young's modulus of $E = 9 \times 10^8$ Pa ($k^* = 0.265$) was selected and simulations were performed for all three plate lengths. The resulting reattachment lengths and base drag coefficients are summarized in Table 9.

Table 9. Effect of plate length on the flow-control performance of elastic plates at $Re_\theta = 5000$ and $E = 9 \times 10^8$ Pa.

L/h	x_r/h	c_B
1.0	6.55	0.218
2.0	5.98	0.204
2.5	6.35	0.200

The results indicate that the medium-length plate ($L/h = 2.0$) produces the shortest reattachment length, whereas the longest plate ($L/h = 2.5$) provides the lowest base drag coefficient. In contrast to the rigid plate results, increasing the plate length does not necessarily improve the flow-control effectiveness. The structural deformation of the longer plates modifies the interaction between the plate and the separated shear layer, resulting in different trends for reattachment length and pressure recovery. To further investigate the influence of structural flexibility, a stiffness sweep was conducted for each plate length. The corresponding results are presented in Table 10.

Table 10. Effect of elasticity on the flow characteristics at $Re_\theta = 5000$.

L/h	Young's Modulus, E (Pa)	k^*	x_r/h	c_B
1.0	2.0×10^8	0.05892	6.50	0.211
1.0	3.0×10^8	0.08837	6.55	0.218
1.0	9.0×10^8	0.26512	6.55	0.218
2.0	3.5×10^8	0.10310	6.26	0.208
2.0	4.0×10^8	0.11783	6.27	0.207
2.0	9.0×10^8	0.26512	5.98	0.204
2.5	9.0×10^8	0.26512	6.35	0.200
2.5	1.0×10^9	0.29458	6.33	0.199
2.5	2.0×10^9	0.58916	6.14	0.196

The stiffness sweep reveals that the influence of elasticity depends strongly on plate length. For the shortest plate ($L/h = 1.0$), variations in Young's modulus produce only minor changes in both reattachment length and base drag coefficient, indicating limited fluid–structure interaction. The plate remains too short to significantly modify the separated shear layer regardless of its flexibility.

For the intermediate plate length ($L/h = 2.0$), the stiffer configuration ($E = 9 \times 10^8$ Pa) provides the best overall performance, yielding the shortest reattachment length of $x_r/h = 5.98$. As the plate becomes more flexible, the reattachment length increases and the pressure recovery deteriorates slightly.

A similar trend is observed for the longest plate ($L/h = 2.5$). Increasing Young's modulus from 9×10^8 Pa to 2×10^9 Pa reduces the reattachment length from 6.35 to 6.14 and decreases the base drag coefficient from 0.200 to 0.196. These results suggest that excessive plate flexibility is detrimental to flow-control performance, whereas a stiffer plate behaves more similarly to the optimum rigid configuration.

Overall, the elastic plates were found to be less effective than the corresponding rigid plates. While fluid–structure interaction modifies the separated flow, the deformation of the plate tends to weaken its ability to constrain the recirculation region and guide the separated shear layer. Consequently, the best performance is obtained for the stiffest elastic configurations, whose behavior approaches that of a rigid plate.

The instantaneous flow fields presented in this section correspond to the elastic plate configuration with $Re_\theta = 5000$, $L/h = 2.5$, and $E = 2 \times 10^9$ Pa ($k^* = 0.58916$). This case represents the stiffest elastic plate investigated and therefore provides insight into the limiting behavior of the fluid–structure interaction system as the structural response approaches that of a rigid plate.

The velocity contour shown in Figure 6a indicates that the elastic plate substantially modifies the separated flow downstream of the step. Similar to the rigid plate configuration, the original recirculation bubble is divided into multiple smaller vortical structures located beneath the plate. The separated shear layer originating from the step corner follows the upper surface of the plate and is redirected toward the lower wall after passing the plate tip. As a consequence, the recirculation region becomes more compact than that of the baseline configuration.

Compared with the rigid plate case, however, the flow remains slightly more elongated downstream of the plate tip. The deformation of the plate alters the local orientation of the control surface and changes the trajectory of the separated shear layer. Although the overall flow topology remains similar to that observed for the rigid plate, the fluid–structure interaction introduces subtle modifications to the vortex organization beneath the plate.

The pressure field presented in Figure 6b exhibits a significant redistribution relative to the baseline flow. A low-pressure region remains immediately downstream of the step, corresponding to the primary separated flow region. However, the extent of this low-pressure zone is reduced due to the presence of the elastic plate. Furthermore, pressure recovery occurs beneath and downstream of the plate, indicating that the flow-control device promotes a partial reduction in the pressure deficit associated with the separated flow.

Nevertheless, the pressure recovery remains weaker than that observed for the corresponding rigid plate configuration. The slight deformation of the plate modifies the local flow acceleration and weakens the formation of a stable high-pressure region downstream of the plate tip. This behavior is consistent with the performance metrics reported previously, where the elastic plate reduced the base drag coefficient relative to the baseline case but remained less effective than the rigid plate of identical length.

The vorticity contour shown in Figure 6c further illustrates the interaction between the elastic plate and the separated shear layer. A concentrated region of negative vorticity originates at the step corner and follows the upper surface of the plate. Beneath the plate, several localized vortical structures are generated, indicating that the plate effectively redistributes the original large-scale recirculation bubble into smaller vortices. Downstream of the plate tip, the vorticity layer gradually weakens as the flow approaches reattachment.

Unlike the baseline configuration, where a single dominant recirculation region occupies most of the separated flow domain, the elastic plate promotes a more fragmented vortex structure and a shorter recirculation zone. However, the vorticity distribution remains somewhat more diffuse than in the rigid plate case, suggesting that the fluid–structure interaction partially weakens the vortex-splitting mechanism responsible for the enhanced pressure recovery observed with the rigid plate.

Overall, the results demonstrate that the elastic cantilevered plate retains the principal flow-control mechanism observed for the rigid configuration, namely the redistribution of the separated shear layer and the reduction in the recirculation region. The present high-stiffness configuration achieves a noticeable improvement relative to the baseline flow by reducing both the reattachment length and the base drag coefficient. However, the slight structural deformation reduces the effectiveness of the pressure recovery process compared with the rigid plate, indicating that excessive flexibility is not necessarily beneficial for passive flow control of backward-facing step flows.

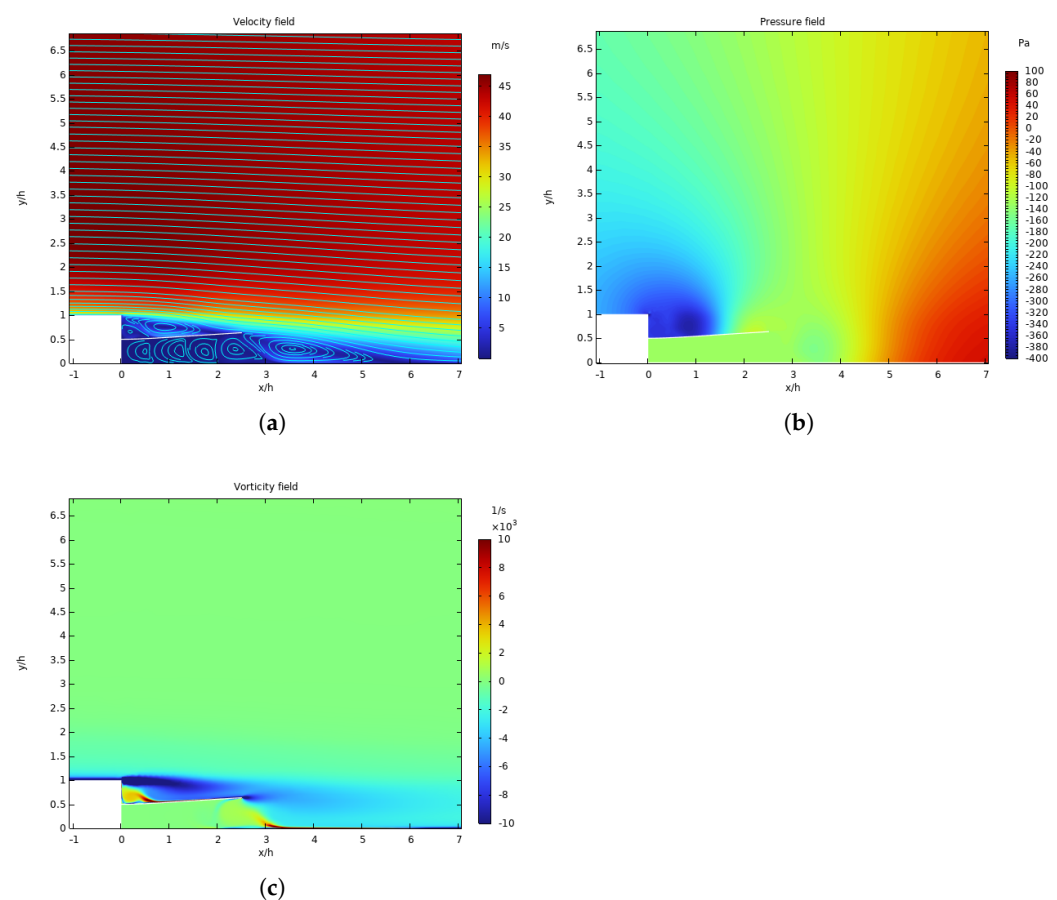


Figure 6. Instantaneous flow-field contours for the elastic plate configuration at $Re_\theta = 5000$, $L/h = 2.5$, and $E = 2 \times 10^9$ Pa ($k^* = 0.58916$): (a) velocity magnitude, (b) pressure field, and (c) vorticity field.

3.4. Temporal Response at $Re_\theta = 5000$

To further examine the transient behavior of the flow-control configurations, the temporal evolution of the non-dimensional reattachment length, x_r/h , base drag coefficient, c_B , and elastic plate tip displacement is evaluated at $Re_\theta = 5000$. The controlled cases correspond to a plate length of $L/h = 2.5$. For the elastic configurations, three Young's modulus values are considered: $E = 9 \times 10^8$ Pa, 1×10^9 Pa, and 2×10^9 Pa.

Figure 7a shows the temporal variation in the reattachment length. The baseline case reaches a steady value of approximately $x_r/h \approx 6.3$, while the rigid plate stabilizes at a

lower value of approximately $x_r/h \approx 5.6$. This confirms that the rigid plate provides a stable reduction in the separation length. The elastic cases initially exhibit an overshoot during the early transient stage before settling to values close to, but generally higher than, the rigid plate case. Among the elastic cases, the stiffest plate, $E = 2 \times 10^9$ Pa, provides the lowest steady reattachment length, indicating that increased stiffness improves the ability of the plate to guide the separated shear layer toward the wall.

Figure 7b presents the corresponding time history of the base drag coefficient. All cases exhibit a pronounced initial transient before approaching a quasi-steady state. The rigid plate consistently yields the lowest steady value of c_B , confirming improved pressure recovery relative to the baseline configuration. The elastic cases settle at values close to the baseline case and remain higher than the rigid plate result. This indicates that although the elastic plates locally modify the reattachment behavior, structural deformation weakens the pressure recovery mechanism achieved by the rigid plate.

The tip displacement histories of the elastic plates are shown in Figure 7c. All elastic cases display a rapid initial deflection followed by a gradual approach to a steady deformation. The lower-stiffness plates exhibit larger final tip displacements, whereas the stiffest plate shows the smallest steady displacement. This behavior confirms that increasing Young's modulus suppresses structural deformation and causes the elastic plate response to approach the rigid plate limit. The combined trends in Figure 7a–c show that excessive flexibility reduces flow-control effectiveness, while higher stiffness improves reattachment behavior and pressure recovery.

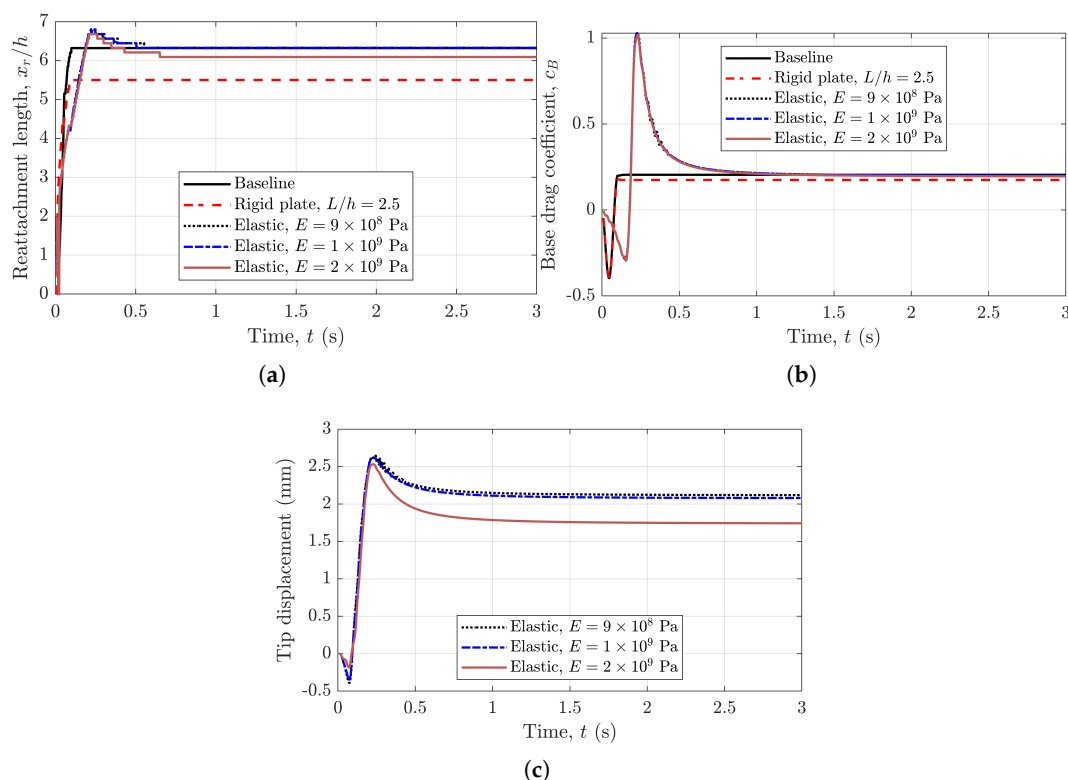


Figure 7. Temporal variation in the flow-control response at $Re_\theta = 5000$ and $L/h = 2.5$: (a) non-dimensional reattachment length, x_r/h , (b) base drag coefficient, c_B , and (c) elastic plate tip displacement for selected stiffness values.

Overall, the temporal response confirms that the rigid plate provides the most stable and effective passive flow-control performance. The elastic plates retain part of the shear-layer-guiding mechanism, but their deformation reduces the ability to sustain the compact recirculation structure and pressure recovery observed in the rigid configuration.

3.5. Limitations of the Present Study

The present investigation was performed using a two-dimensional URANS framework coupled with a two-way fluid–structure interaction model. Although the adopted methodology was validated against the benchmark experimental data of Driver and Seegmiller and successfully reproduced the primary flow characteristics, including the reattachment length, pressure recovery, and wall shear stress distribution, certain limitations remain. In particular, the two-dimensional formulation cannot capture inherently three-dimensional flow structures that may develop within the separated shear layer and recirculation region. Furthermore, the URANS approach resolves the dominant unsteady flow motions while modeling the smaller-scale turbulent structures. Consequently, detailed vortex dynamics and broadband turbulence characteristics associated with the fluid–structure interaction process may not be fully represented.

4. Conclusions

This study investigated the passive control of backward-facing step flows using rigid and elastic cantilevered plates. The effectiveness of the proposed control strategy was assessed using the non-dimensional reattachment length and base drag coefficient, which respectively characterize the extent of the recirculation region and the pressure recovery downstream of the step.

The rigid plate configurations were found to provide the most effective flow-control performance. For all Reynolds numbers considered, increasing the plate length reduced the reattachment length and improved pressure recovery. The longest plate investigated ($L/h = 2.5$) consistently produced the lowest reattachment lengths and base drag coefficients. Flow-field visualizations revealed that the plate modifies the separated shear layer, promotes vortex splitting, and generates secondary recirculation regions that weaken the primary separation bubble and accelerate flow reattachment.

The fluid–structure interaction simulations demonstrated that plate stiffness plays a critical role in determining flow-control effectiveness. While all elastic plates altered the separated flow structure, increasing structural stiffness resulted in improved performance. The stiffest elastic configuration produced lower reattachment lengths and base drag coefficients than the more flexible cases, indicating that excessive deformation weakens the ability of the plate to guide the separated shear layer and sustain pressure recovery.

The transient analysis further highlighted the differences between rigid and elastic control strategies. The rigid plate exhibited a rapid and stable convergence toward its steady-state solution, whereas the elastic plates displayed pronounced transient overshoots in reattachment length, base drag coefficient, and tip displacement before reaching equilibrium. These additional dynamics originated from the coupled interaction between the deforming structure and the separated flow field.

Overall, the results demonstrate that rigid cantilevered plates constitute an effective passive flow-control strategy for the backward-facing step configuration investigated in the present study, whereas elastic plates provide more limited improvements that depend strongly on structural stiffness. Among the configurations considered, rigid plates yielded the greatest simultaneous reduction in reattachment length and base drag coefficient, while elastic plates required sufficiently high bending stiffness to approach the performance of the rigid configuration. The findings indicate that successful passive flow control depends not only on the geometric placement of the control device but also on maintaining an appropriate balance between structural flexibility and flow-induced loading.

The present study further highlights the importance of non-dimensional bending stiffness as a governing parameter in fluid–structure interaction-based flow control and demonstrates that reductions in reattachment length do not necessarily correspond to im-

proved pressure recovery. The conclusions drawn herein are limited to the backward-facing step configuration and Reynolds number range investigated. Additional three-dimensional numerical and experimental studies would be required to assess the applicability of these findings to practical aerodynamic systems.

The present study provides a systematic comparison between rigid and elastic cantilevered control elements in an externally developing backward-facing step flow and highlights the importance of non-dimensional bending stiffness as a governing parameter for flow-control effectiveness. Future work should focus on three-dimensional turbulence-resolving simulations, such as LES or hybrid RANS–LES approaches, to further investigate the detailed vortex dynamics and fluid–structure interaction mechanisms associated with deformable passive flow-control devices.

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