
Research article

Hydrogen feed reduction in small-scale open-cathode PEMFCs using passive ejector-based recirculation

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Abstract: This work presents an experimental investigation of a simple ejector-based passive hydrogen recirculation system for a 0.5 kW small-scale open-cathode proton exchange membrane fuel cell (PEMFC) stack. Several ejector geometries were designed and fabricated using a 3D printer, and the final configuration employed a converging nozzle with a diameter of 0.2 mm and a mixing-chamber-to-nozzle diameter ratio of 4.5. Experiments showed that the ejector achieves entrainment ratios peaking at ~ 1.3 at low current and stabilizing around 0.9–1.0 at higher loads, while maintaining stable operation across the full load range. Compared with a dead-ended anode configuration, passive recirculation reduces the total hydrogen supply required by the system, with the reduction being strongly influenced by purge dynamics that are prominent in sub-kW operation. Beyond averaged hydrogen saving, purge-event transients were quantified using time-resolved supply-flow traces and introduced purge-event metrics (overshoot amplitude and purge-induced excess volume) to capture purge-dominated behavior and the buffering role of recirculation. Finally, by contrasting without and with anode inlet pressure setting operations, this paper shows that pressure regulation can improve steady utilization yet partially compromise transient suppression, motivating purge-aware pressure control as a practical operational guideline for small-scale open-cathode PEMFC systems.

Keywords: recirculation; open cathode PEMFC; micro-ejector; hydrogen utilization; experimental validation

1. Introduction

The anode gas supply and recirculation system play a central role in determining the efficiency, durability, and compactness of proton exchange membrane fuel cell (PEMFC) systems, especially in the low-power range. In addition to the electrochemical stack, the balance of plant (BoP) must manage hydrogen and air flow, water production, heat removal, and purge events in a coordinated way in order to avoid fuel starvation, flooding, or drying of the membrane [1,2]. For small and medium-scale systems (typically below 5–6 kW), air-cooled open-cathode PEMFC configurations are attractive because they use the same air stream for both reactant supply and cooling, thereby simplifying the BoP and reducing parasitic power consumption [3–5]. Recent reviews and experimental studies on open-cathode PEMFCs have highlighted that non-uniform reactant distribution, temperature gradients, and limited water-management margins can significantly degrade performance and lifetime, and that these issues become more severe at low loads and in compact stacks [3–6]. Related experimental studies on compact PEMFC stacks have further demonstrated the sensitivity of cell-to-cell performance and degradation to non-uniform reactant and thermal conditions. He et al. showed that cathode starvation and localized ice accumulation can lead to voltage divergence, reversal, and measurable degradation during severe cold-start operation [7], while subsequent work on a short PEMFC stack highlighted the importance of maintaining internal thermal uniformity to prevent localized performance deterioration [8].

On the anode side, three basic fuel-feed architectures are commonly used: dead-ended, semi-dead-ended (or “flow-through”), and open-ended designs [1]. Dead-ended operation is particularly attractive in small systems because it maximizes apparent fuel utilization, but it also leads to progressive accumulation of inert gases (primarily nitrogen crossover from the cathode) and liquid water in the anode compartment, which in turn causes voltage decay, local starvation, and accelerated degradation [9,10]. Consequently, dead-ended systems require periodic purge events, whose timing and duration have to be optimized to balance hydrogen losses against the removal of nitrogen and liquid water. Purge-strategy optimization and anode purge management have therefore become key research topics, with several works demonstrating that purge interval, purge duration, and purge trigger criteria (e.g., voltage-based or nitrogen-based) have a strong impact on hydrogen utilization, efficiency, and degradation rate [2,10,11]. At the same time, these studies show that even well-tuned purge strategies inevitably waste part of the supplied hydrogen, especially at low loads where the ratio between purge flow and electrochemically consumed fuel becomes unfavorable [1,2].

To mitigate purge-related hydrogen losses and improve anode gas distribution, many commercial PEMFC systems employ an anode recirculation loop [2,12]. In such systems, a portion of the anode exhaust gas—containing unreacted hydrogen, water vapor, and inert species—is redirected back to the anode inlet, so that fresh hydrogen is mixed with recirculated gas instead of being supplied in once-through mode. This approach increases effective hydrogen utilization, can homogenize anode concentration and humidity, and reduces the frequency and severity of purge events, provided that nitrogen enrichment in the loop is controlled [1,10].

In the last decade, ejector-based anode recirculation has therefore emerged as one of the most promising passive solutions for PEMFC hydrogen management. Arabbeiki et al. [13] summarized the state of the art on ejector geometries, operating principles, and numerical modeling, concluding that ejectors are now widely recognized as key components in PEMFC hydrogen recirculation loops, but that their application is often limited by a narrow operating range and sensitivity to primary mass-flow

and pressure conditions. A number of studies have focused on medium- and high-power systems. For example, Nikiforow et al. [14] designed, 3D-printed, and experimentally characterized a hydrogen recirculation ejector for a 5 kW stationary PEMFC system, emphasizing high turn-down-ratio operation and the influence of humid hydrogen on entrainment behavior. Kuo and Hsieh [15] numerically investigated the effects of ejector geometry and operating conditions on the hydrogen recirculation ratio in an 80 kW automotive-type PEMFC system, highlighting how nozzle throat diameter, mixing-chamber length, and back-pressure govern entrainment ratio and system pressure drops. Pei et al. [16] analyzed a wide-operating-range ejector based on anode pressure-drop characteristics, while Han et al. [17] studied multi-nozzle ejectors to extend the operating window and improve robustness against variations in secondary-side pressure and humidity.

More recent work has combined detailed numerical modeling with experimental validation to quantify the energy efficiency and dynamic behavior of hydrogen recirculation ejectors at the system level. Ding et al. [18] evaluated the energy efficiency of hydrogen recirculation ejectors in PEMFC systems and showed how ejector design and operating point influence net system efficiency once parasitic losses are accounted for. Feng et al. [19] investigated the design of hydrogen recirculation ejectors across wide load ranges and reported that the entrainment ratio and recirculation performance deteriorate markedly at low primary mass-flow rates, corresponding to low stack power, unless geometry and operating conditions are carefully optimized. Singer et al. [20] developed and validated a 2D-CFD-based design procedure for passive hydrogen recirculation ejectors in a 5 kW PEMFC system, focusing on geometric optimization and the impact of humidity and pressure on performance. Collectively, these works demonstrate that ejector-based recirculation can achieve high hydrogen utilization over relatively broad operating ranges in kW-scale stacks, but that achieving stable entrainment under very low power conditions remains challenging [13,18,19].

In parallel, the literature on open-cathode PEMFCs has largely concentrated on air-side management, thermal behavior, and stack performance under various environmental conditions. However, most of these studies assume relatively simple anode supply configurations (dead-ended or flow-through) and do not explicitly address the design and control of anode hydrogen recirculation hardware. Only a few works have investigated hydrogen flow-control strategies tailored to open-cathode stacks—for example, control of hydrogen flow to improve efficiency and protect the membrane—but these studies still rely on conventional non-recirculating anode architectures [21,22].

The existing literature indicates that (a) anode recirculation and purge strategies are critical for hydrogen utilization and durability in PEMFC systems, particularly under dead-ended operation; (b) ejector-based passive recirculation is attractive for minimizing parasitic loads and simplifying the balance of plant (BoP); and (c) experimental comparisons between ejector-based passive recirculation and dead-ended operation have predominantly focused on closed-cathode, kW-scale automotive or stationary systems operating with relatively stable anode pressures and higher hydrogen flow rates [13,18,19,23–26]. However, the operating regime of sub-kW air-cooled open-cathode PEMFCs differs in ways that can strongly alter both ejector behavior and the interpretation of hydrogen savings: Low absolute flow rates and limited pressure margins make entrainment more sensitive to backpressure variations, while fixed periodic purging can contribute a disproportionate transient component to the measured hydrogen withdrawal, especially at low-to-mid loads.

The comparison in Table 1 shows that prior ejector-based hydrogen-recirculation studies predominantly emphasize ejector geometry, steady entrainment, or wide-load operability in kW-to tens-of-kW PEMFC systems. In contrast, fixed periodic purging and very low absolute hydrogen flow

can dominate the measured fuel-management behavior of a sub-kW open-cathode stack. To the best of the authors' knowledge, prior work has not systematically examined passive ejector-based anode recirculation in a sub-kilowatt air-cooled open-cathode PEMFC while explicitly resolving purge-event dynamics and comparing the resulting operating modes with a dead-ended baseline. Accordingly, this work does not claim a new ejector aerodynamic principle; its novelty lies in an experimentally grounded, purge-aware assessment framework for a 0.5 kW open-cathode stack. The contributions are: (i) experimental characterization of a micro-ejector sized for the low-flow/low-pressure operating envelope; (ii) time-resolved quantification of purge-event transients using dimensional and normalized metrics; and (iii) evaluation of the interaction between passive recirculation and the anode inlet-pressure trajectory. Two recirculation modes are used throughout the manuscript: Rec0 denotes passive recirculation without an imposed anode inlet-pressure setpoint, whereas RecP denotes recirculation with the inlet pressure manually regulated toward a target of 0.5 barg. Both modes use the same ejector hardware and purge schedule.

Table 1. Representative comparison of prior PEMFC ejector-recirculation studies with the present sub-kW open-cathode study.

Reference	Scale	Cathode configuration	Approach	Primary focus	Purge-resolved analysis	Distinction from present study
Nikiforow et al. [14]	5 kW	Not emphasized	CFD + experiment	Ejector sizing; turn-down operation; humid H ₂	No	Higher-power stationary system
Kuo and Hsieh [15]	80 kW	Automotive PEMFC; cathode type not central	Numerical	Geometry, back-pressure, recirculation ratio	No	Much higher flow and pressure range
Singer et al. [20]	5 kW	Not emphasized	2D CFD + validation	Passive ejector design; humidity and pressure effects	No	Design-centered kW-scale study
Wang et al. [24]	10 kW	Not emphasized	Experiment	Anode ejector recirculation performance	No	Higher-power experimental benchmark
Present study	0.5 kW	Open cathode	Experiment; purge-resolved analysis	Low-flow ejector feasibility; DEA comparison; purge transients; inlet-pressure trajectory	Yes	Sub-kW, purge-dominated operating regime

2. System description

2.1. PEMFC with dead-ended anode

The experiments were carried out on a 500 W open-cathode PEMFC stack consisting of 24 cells. The stack is air-cooled using an integrated cooling fan and is specified for operation up to 65 °C, with a nominal anode hydrogen pressure of 0.45–0.55 bar gauge and a hydrogen purity of at least 99.995% dry H₂. At maximum output, the stack requires a hydrogen flow rate of 6.5 L/min and is protected by

shutdown limits of 12 V (low voltage), 42 A (over-current), and 65 °C (over-temperature). An external 13 V, 5 A DC power supply is used to power the internal controller and auxiliaries.

The PEMFC test bench is completed with a hydrogen cylinder as the fuel source, a mass flow controller (MFC) to adjust the hydrogen flow rate according to the stack current, and a pressure regulator at the cylinder outlet to set the stack inlet pressure. A pressure indicator (PI) is installed to monitor the hydrogen pressure at both the inlet and outlet channels, while a flow transmitter (FT) is used to measure the hydrogen flow at these locations. A solenoid valve (S) is employed for anode purging, and a programmable DC load is used to impose the electrical load on the stack. The overall experimental setup is shown in Figure 1(a).

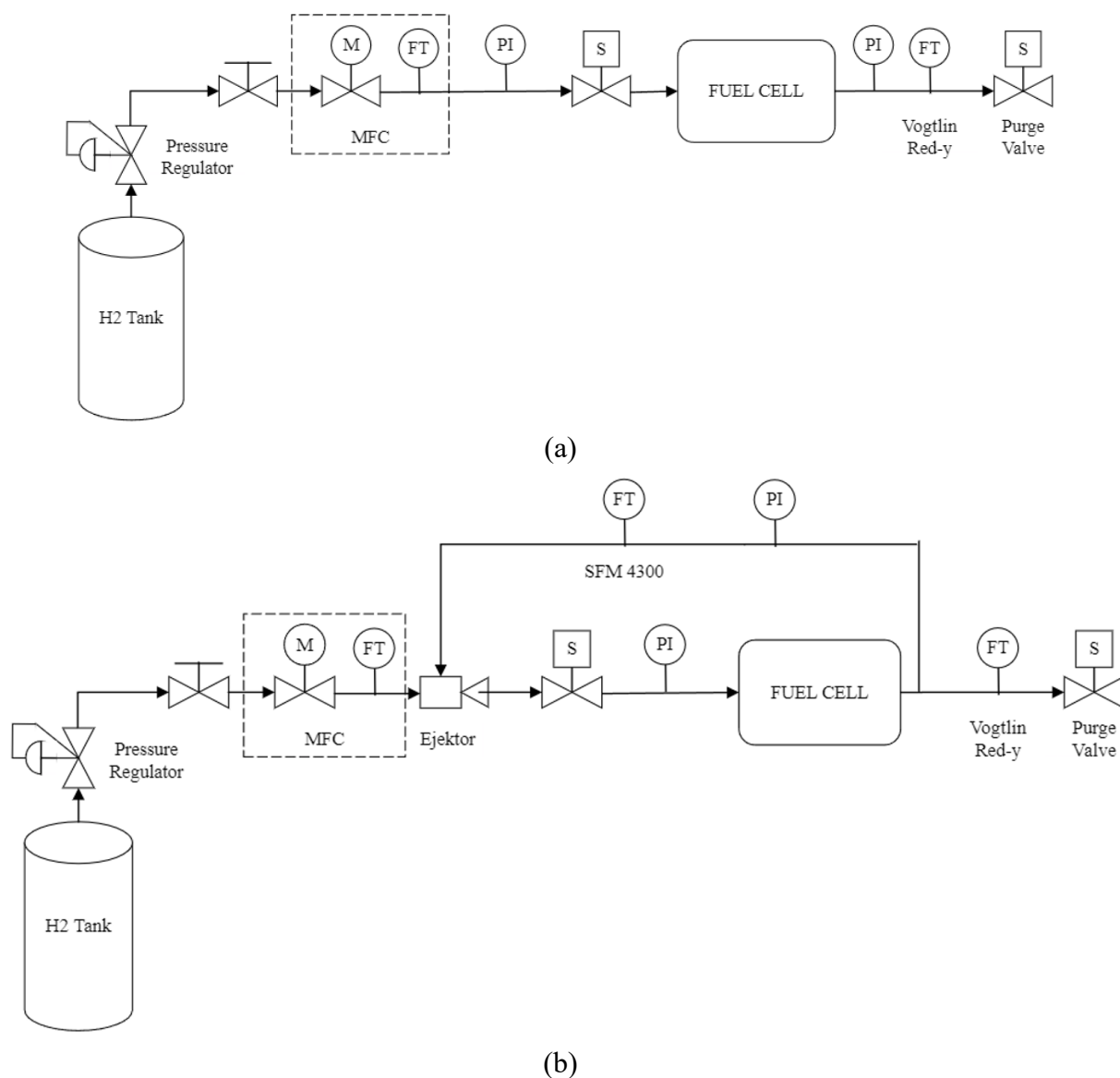


Figure 1. Schematic of the open cathode PEMFC: (a) dead-ended scheme; (b) the anode recirculation scheme.

In addition to regulating the hydrogen mass flow rate, the MFC is also used to measure the inlet hydrogen flow. The hydrogen flow at the outlet channel of the PEMFC is measured by a Vogtlin

Red-y Compact Series flowmeter, which is rated for a maximum operating pressure of 10 barg and a temperature range of 0–50 °C. This device provides a standardized volumetric flow that is independent of the instantaneous gas pressure and temperature and is equipped with a totalizer function that records the cumulative gas volume during operation. The PEMFC is connected to a programmable DC load operated in constant-current mode to impose different output current levels. Data acquisition is performed using an NI USB-6001 data acquisition (DAQ) module, which records the stack voltage, MFC flow signal, and SFM4300 flowmeter signal in real time. The DAQ is also used to drive the MFC control signal so that its valve is fully opened (100%) during the experiments. Stack voltage is measured via a resistive voltage divider, which attenuates the voltage to a level compatible with the DAQ input range.

2.2. Performance measurement

The methodology used to quantify hydrogen consumption is based on measured fresh-hydrogen supply and anode-discharge flow signals. Species-resolved analysis of the anode discharge was not available; therefore, the measured outlet stream should be interpreted as a total gas stream that may contain H₂, nitrogen from cathode-to-anode crossover, and water vapor. Published work shows that nitrogen crossover and anode nitrogen accumulation depend strongly on operating pressure, stoichiometry, membrane properties, and purge/recirculation conditions [27,28]. Consequently, the non-hydrogen fraction is treated here only as an illustrative benchmark rather than as a measured composition. Accordingly, rather than assigning a single representative value, the present study evaluates 2%, 5%, and 15% as sensitivity cases. If x is the total non-H₂ fraction in the exhaust—including nitrogen and water vapor—then

$$V_{waste,H_2} = (1 - x)V_{waste,total} \quad (1)$$

All measurements were conducted after the open-cathode PEMFC had been operated for 5 min at each load current (3, 6, 9, 12, 15, and 18 A) to ensure quasi-steady conditions. Each operating condition was evaluated in three independent 300-s experimental runs. The three runs were used as the independent experimental repetitions for reporting steady and integrated performance metrics. Using the averaged signals at each current level, the hydrogen volumes associated with feed, reaction, and purge were calculated, and the hydrogen utilization was then obtained as:

$$\eta_{H_2} = \frac{V_{react,H_2}}{V_{feed,H_2}} = \frac{V_{feed,H_2} - V_{waste,H_2}}{V_{feed,H_2}} = 1 - \frac{(1-x)V_{waste,total}}{V_{feed,H_2}} = \eta_{H_{2,0}} + x(1 - \eta_{H_{2,0}}) \quad (2)$$

where V_{react,H_2} is the amount of hydrogen used (mL), V_{feed,H_2} is the amount of hydrogen supplied from the tank (mL), V_{waste,H_2} is the amount of hydrogen released into the environment during purging (mL), and $\eta_{H_{2,0}}$ is the hydrogen utilization for $x = 0$. Therefore, the composition-induced deviation in hydrogen utilization is given by

$$\Delta\eta_{H_2} = x(1 - \eta_{H_{2,0}})$$

For an assumed non-hydrogen fraction in the range $x = 2\% - 15\%$, the corresponding hydrogen-utilization interval is

$$\eta_{H_2} \in [\eta_{H_{2,0}} + 0.02(1 - \eta_{H_{2,0}}), \eta_{H_{2,0}} + 0.15(1 - \eta_{H_{2,0}})]$$

To quantify the reduction in fresh-hydrogen consumption relative to the dead-ended baseline, the hydrogen-feed volume reduction is defined as

$$R_{V,j} = \frac{V_{DE} - V_j}{V_{DE}} \times 100\% \quad (3)$$

where V_{DE} is the integrated fresh-hydrogen feed volume measured during dead-ended operation, and V_j is the corresponding integrated fresh-hydrogen feed volume for recirculation mode (Rec0 or RecP) over the same 5 min measurement period. Positive values of $R_{V,j}$, therefore, indicate a reduction in fresh-hydrogen supply relative to the dead-ended baseline.

Note that x affects the absolute hydrogen utilization (and later effective-entrainment values) but does not alter the qualitative comparison among the tested operating modes. The feed-based system efficiency defined in Eq (2) and the hydrogen-feed volume reduction defined in Eq (3) are calculated directly from the measured fresh-hydrogen supply and are therefore independent of the assumed exhaust composition. For instance, if the baseline utilization $\eta_{H_2,0}$ is 0.90, the hydrogen utilization values for $x = 2\%$, 5% , and 15% are 0.902, 0.905, and 0.915, respectively. This means that a change in the non- H_2 component from 2% to 15% alters the utilization by only about 1.3 percentage points against a baseline of 0.90.

For each load condition, the outlet flow rate at the hydrogen tank and the stack voltage were recorded as functions of time. The resulting total hydrogen volume, together with the electrical energy generated, was subsequently used to evaluate the fuel cell system efficiency according to the following equation:

$$\eta_{FC} = \frac{E_{stack}}{E_{feed,H_2}} = \frac{E_{stack}}{HHV V_{feed,H_2} r_{feed,H_2}} \quad (4)$$

where E_{stack} is the electrical energy generated by the PEMFC stack (Wh), E_{feed,H_2} is the chemical energy of the fresh hydrogen supplied to the PEMFC (Wh), HHV is the higher heating value of hydrogen (39,400 Wh/kg), and r_{feed,H_2} is the density of hydrogen at these conditions (8.38×10^{-8} kg/mL). The metric in Eq (4) represents gross fuel-to-stack electrical efficiency based on fresh hydrogen HHV. It does not include upstream hydrogen compression/storage energy, pressure-exergy destruction across the regulator/ejector, or auxiliary BoP (Balance of Plant) power, and should therefore not be interpreted as a complete net system efficiency. In the present test bench, hydrogen is supplied from a pre-pressurized cylinder through a passive mechanical regulator; therefore, no electrically driven upstream compression/regulation power was measured.

2.3. Purge-transient data processing and performance metrics

In addition to the steady and integrated performance metrics described above, the time-resolved hydrogen-supply signal was analyzed to quantify the transient response associated with periodic anode purging. The purge schedule of the tested stack was fixed for all operating modes and load conditions, with the purge valve opening every 10 s for approximately 0.5 s. Accordingly, each 300-s run contained approximately 30 purge events. Overshoot amplitude and purge-induced excess volume were calculated separately for each event to characterize event-to-event variability. The purge events within a run were treated as repeated observations nested within that experimental run and were not considered independent experimental replicates.

For each independent experimental run, the baseline hydrogen-supply flow (q_b) was defined as the median MFC flow over the corresponding 300-s record. The median was used rather than the arithmetic mean to reduce sensitivity to the short-duration purge-induced flow peaks. Each record was then divided into individual purge cycles of duration $T_c = 10$ s. For the k -th purge cycle, the peak flow q_{peak} was defined as the maximum measured supply flow within that cycle. The purge-induced overshoot amplitude was calculated as

$$A_{OS} = q_{peak} - q_b \quad (5)$$

where A_{OS} represents the absolute increase in hydrogen-supply flow above the baseline level during a purge event. A smaller value, therefore, indicates stronger suppression of the purge-induced supply-flow excursion.

To quantify the additional hydrogen withdrawal associated with purge recovery, the purge-induced excess volume for each event was calculated by integrating only the positive deviation of the supply-flow signal from its baseline value over one purge cycle:

$$V_{ex} = \int_{t_0}^{t_0+T_c} \max(q - q_b, 0) dt \quad (6)$$

where t_0 denotes the beginning of the k -th purge cycle. V_{ex} , therefore, represents the additional hydrogen volume drawn from the fresh-hydrogen supply above the nominal baseline delivery during that purge cycle.

Because the dimensional values of overshoot amplitude and excess volume depend on stack size, operating load, and baseline hydrogen demand, normalized dimensionless forms were additionally introduced to facilitate comparisons across operating conditions and fuel-cell systems. The normalized overshoot amplitude is defined as

$$A_{OS}^* = \frac{q_{peak} - q_b}{q_b} \quad (7)$$

while the normalized purge-induced excess volume is defined as

$$V_{ex}^* = \frac{V_{ex}}{q_b T_c} \quad (8)$$

Thus, A_{OS}^* represents the purge-induced peak-flow excursion relative to the baseline hydrogen-supply flow, whereas V_{ex}^* represents the additional purge-recovery volume relative to the nominal hydrogen volume that would be supplied at the baseline flow rate during one purge cycle.

To account for event-to-event fluctuations, the above metrics were evaluated separately for each purge event rather than treating the complete 300-s record as a single transient observation. For each experimental run, the event-level values were first summarized over the approximately 30 purge cycles. The resulting run-level statistics were then summarized across the three independent experimental repetitions for each operating condition. Individual purge events within the same experimental run were therefore treated as repeated observations nested within that run and were not considered independent experimental replicates. Reported values are presented as the mean across the three independent runs together with the corresponding variability.

This event-resolved procedure provides complementary information to the integrated hydrogen-consumption analysis. While the integrated metrics quantify the overall fresh-hydrogen

requirement over the 5-min operating period, A_{OS} and V_{ex} characterize the magnitude of purge-induced disturbances, and their normalized counterparts A_{OS}^* and V_{ex}^* allow the transient buffering performance of the different operating modes to be compared independently of the absolute hydrogen-flow level.

3. Passive anode recirculation system

3.1. Passive anode recirculation configuration and operation

The proposed anode recirculation system is a passive configuration that uses an ejector to mix the secondary stream returning from the anode side with fresh hydrogen supplied from the tank. The recirculation-line flow measurement is therefore reported as a secondary-stream flow rather than as a species-resolved hydrogen flow. To quantify the amount of hydrogen being recirculated, an SFM4300 flowmeter is installed in the recirculation line. The hydrogen leaving the system is measured by a Vogtlin Red-y Compact Series flowmeter [with a resolution of 0.001 standard liters per minute (slpm)] mounted at the fuel cell outlet downstream of the recirculation loop. In addition, pressure gauges are placed on both the anode inlet line and the recirculation line to monitor the pressure conditions. The schematic of the passive anode recirculation system is shown in Figure 1(b).

The operation of the passive anode recirculation system is controlled by the built-in manufacturer controller of the H-500. During the experiments, the anode inlet pressure is manually maintained within the range of 0.4–0.6 barg. This pressure regulation is achieved using the pressure regulator installed on the hydrogen tank. The setpoint on this regulator must be higher than the desired anode inlet pressure, since the ejector introduces a pressure drop across the nozzle and mixing section.

The incorporation of an ejector into the passive anode recirculation system creates a pressure difference between the outlet of the tank regulator and the anode inlet, which increases with the stack load. These effects impose specific design requirements on the ejector, particularly for small-scale PEMFC systems below 1 kW, where the available supply pressure is limited and the pressure margin is narrow. Therefore, the ejector geometry must be selected so that a sufficient entrainment ratio is achieved at the minimum allowable supply pressure (namely 0.45–0.6 barg), while limiting the pressure penalty (approximately 0.10–0.15 bar at all load conditions) and preserving a stable anode inlet pressure dynamic. In practice, this means that the nozzle diameter, mixing-chamber diameter, and diffuser geometry have to be selected specifically for the low-flow, low-pressure operating range of small-scale open-cathode PEMFC stacks, rather than directly adopting designs developed for larger, high-pressure stacks.

3.2. Ejector design, sizing, and performance metrics

In this study, the ejector geometry was designed in Autodesk Fusion 360 and fabricated by 3D printing. The primary-nozzle diameter D_n is a key design variable because it determines the minimum primary mass flow rate and primary inlet pressure required to initiate suction from the recirculation line. Reducing D_n increases the restriction of the primary nozzle and, for the available low-flow/low-pressure operating envelope, can promote a sufficiently energetic primary jet and stronger static-pressure depression in the suction chamber [9], but it also increases pressure loss and manufacturing sensitivity. The nozzle diameter was therefore varied within a theoretically constrained and practically

manufacturable range.

The initial nozzle sizing was guided by compressible-flow relations for choked flow through a nozzle. The theoretical basis used to estimate the nozzle diameter is given by [14]:

$$D_n = \frac{\dot{n}_{p,in} M_{w,p}^4}{C \pi p_{p,in} 10^5 \sqrt{\gamma \frac{M_{w,p}}{R(T_{p,in} + 273.15)} \left(\frac{2}{\gamma+1}\right)^{\frac{\gamma+1}{\gamma-1}}}} \quad (9)$$

where D_n is the nozzle diameter (m), $\dot{n}_{p,in}$ is the maximum hydrogen consumption (mol/s), $M_{w,p}$ is the molar mass of hydrogen gas (0.002016 kg/mol), C is the coefficient of nozzle discharge (assumed to be 0.95), $p_{p,in}$ is the primary flow pressure in absolute bar, γ is the isentropic expansion factor of hydrogen (namely 1.4), R is the gas constant (8.3145 J/(K.mol)), and $T_{p,in}$ is the primary flow temperature, which is assumed to be 300 K.

The maximum hydrogen molar flow rate was estimated from the stack current using Faraday's law:

$$\dot{n}_{p,in} = \frac{N_{cell} I}{2F} \quad (10)$$

where N_{cell} is the number of cells in the stack, I is the stack current, and F is Faraday's constant (96485 C/mol). In this study, $N_{cell} = 24$ and a maximum current of 20 A was used for sizing, consistent with the rated operating envelope of the stack.

Unless stated otherwise, pressures reported in the experimental section are gauge pressures (barg). For nozzle sizing, $p_{p,in}$ in Eq (9) is expressed as absolute pressure [bar(a)] for consistency with the compressible-flow formulation (gauge values can be obtained by subtracting ambient pressure). The primary inlet pressure was considered over $p_{p,in} = 1.5, 2.0, 2.5$, and 3.0 bar(a), yielding an estimated nozzle-diameter range of approximately 0.2–0.3 mm. Based on both this range and the printer resolution, nozzle diameters of 0.2 and 0.3 mm were selected for testing. A 1.0 mm nozzle was also evaluated to provide a reference case commonly adopted in prior studies.

The ejector geometry was not selected purely by trial-and-error. The design was first constrained by the expected low-pressure/low-flow operating window of the 0.5 kW stack and by compressible-flow relations, and then a limited set of manufacturable geometries was screened under the resolution constraints of 3D printing. Because the explored design space is necessarily limited, the resulting conclusions are presented as guidance for the tested operating window rather than universal optima; broader generalization will require expanded geometric sweeps and/or validated CFD.

The mixing-chamber diameter D_m was selected proportional to D_n , and the mixing length L_m was tuned for each nozzle configuration using commonly recommended geometric-ratio windows D_m/D_n and L_m/D_m (typically 3–6) [9]. Larger ratios tend to favor performance at higher flow rates, whereas smaller ratios can be beneficial at lower flow rates. The nozzle-exit position relative to the mixing chamber (NXP, see Figure 2) also influences entrainment and was therefore kept within a short distance ($NXP < 4$ mm) to promote stable suction under the targeted operating conditions [15].

With respect to the diffuser, prior parametric studies indicate that once the diffuser geometry is within a reasonable design window, variations in diffuser length and divergence angle generally have a smaller influence on entrainment than the primary nozzle and mixing-chamber diameters [27]. Accordingly, the diffuser length was kept constant at 11.5 mm across all variants. The diffuser angle was adjusted to meet a fixed diffuser outlet diameter of 6 mm, chosen to simplify manufacturing and assembly while remaining within literature-recommended diffuser-angle ranges [27].

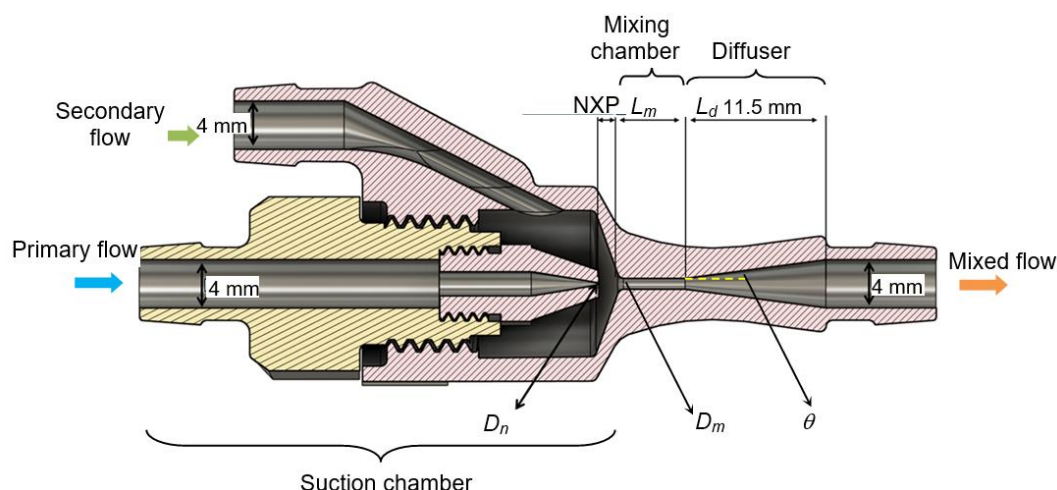


Figure 2. Structure sketch of the proposed ejector.

Ejector performance is commonly characterized by the entrainment ratio, defined as the ratio of secondary to primary flow rate. In this paper, the conventional total entrainment ratio is reported as:

$$\varepsilon_{tot} = \frac{\dot{q}_s}{\dot{q}_p} \quad \varepsilon_{H_2} = \frac{\dot{q}_{s,H_2}}{\dot{q}_{p,H_2}} = (1 - x) \varepsilon_{tot} \quad (11)$$

where $\dot{q}_s$ and $\dot{q}_p$ are the secondary and primary volumetric flow rates, respectively. The secondary stream may contain hydrogen as well as nitrogen (crossover/leakage) and water vapor. Therefore, ε_{tot} is therefore interpreted as a measured total-secondary-stream entrainment ratio, not as a direct hydrogen recirculation ratio. To better reflect hydrogen recirculation performance, this research additionally defines a hydrogen entrainment ratio ε_{H_2} . Because the gas composition is not measured directly in the present setup, ε_{H_2} is reported as a bounded estimate based on plausible hydrogen fractions in the secondary stream. For assumed non-hydrogen fractions of 2%, 5%, and 15%, the corresponding multiplicative correction factors applied are 0.98, 0.95, and 0.85, respectively; thus, $\varepsilon_{H_2} = 0.98 \varepsilon_{tot}$, $\varepsilon_{H_2} = 0.95 \varepsilon_{tot}$, and $\varepsilon_{H_2} = 0.85 \varepsilon_{tot}$, respectively.

Finally, a minimum entrainment ratio is often used as a practical criterion indicating that suction is sustained. However, such threshold values (e.g., $\varepsilon_{tot} \geq 0.2$) should be interpreted only as a conventional suction/operability indicator rather than a direct measure of hydrogen recirculation effectiveness. In this study, ejector suitability for passive anode recirculation is assessed based on sustained suction over the full operating range of interest, together with system-level performance (hydrogen savings and purge-transient suppression) under the tested conditions.

4. Experimental results

4.1. Ejector performance

As mentioned earlier, three nozzle diameters D_n were tested. For these three values, corresponding values of D_m and L_m were selected such that the geometric ratios D_m/D_n and L_m/D_m fall within the range 3–6. This procedure resulted in five different ejector configurations being manufactured and tested, as summarized in Table 2. The last column of the table reports the

experimental outcome for each ejector configuration.

For the large nozzle diameter of $D_n = 1$ mm (variations #1 and #2), two different nozzle-exit positions ($NXP = 3.3$ and 2 mm) were examined. The reduction in NXP was intended to increase the velocity of the primary jet entering the mixing chamber and thereby enhance the pressure drop in the suction chamber, so that flow from the recirculation line could be induced. However, both configurations share the same relatively small geometric ratios ($D_m/D_n = 2$, $L_m/D_m = 2$), and the experimental results show that neither variation was able to generate any measurable secondary flow. The area ratio between the nozzle and mixing chamber remained too small to produce a sufficiently high jet velocity and local pressure depression at the available supply pressures, so the ejector could not entrain hydrogen from the recirculation line.

For the small nozzle diameters of 0.3 and 0.2 mm, three additional variations were designed. Variations #3 and #4 use the same mixing-chamber, diffuser, and suction-chamber geometry, but differ in nozzle diameter ($D_n = 0.3$ mm and $D_n = 0.2$ mm, respectively). Variation #5 employs the same geometric ratios and NXP as Variation #3, but with a smaller mixing-chamber diameter ($D_m = 0.6$ mm). In practice, the mixing-chamber diameter in Variation #5 proved too small for the 3D-printing process used, so the mixing hole could not be fabricated, and this configuration was not tested.

Manufacturability of the micro-ejector is constrained by the printer–resin–process combination. The present process uses a nominal layer thickness of 0.01 – 0.20 mm and an empirical minimum reliable internal-channel feature d_{min} of approximately 0.3 – 0.4 mm. To quantify the fabrication margin, a dimensionless printability ratio $\Pi_p = D_m/d_{min}$ is introduced. For Variant #4 ($D_m = 0.9$ mm), $\Pi_p = 2.25$ – 3.0 ; for Variant #5 ($D_m = 0.6$ mm), Π_p decreases to 1.5 – 2.0 . In addition, the cross-sectional area of the Variant #5 mixing chamber is around 0.44 of Variant #4, leaving substantially less clearance for resin drainage and increasing susceptibility to resin retention, overcuring, and shrinkage. These limits are process-specific and should not be interpreted as universal minimum printable dimensions.

Table 2. Variations of ejector experiments and their results.

Variation	D_n (mm)	D_m (mm)	L_m (mm)	q (deg)	NXP (mm)	D_m/D_n	L_m/D_m	Entrainment ratio/information
#1	1	2	4	5	3.3	2	2	0
#2	1	2	4	5	2	2	2	0
#3	0.3	0.9	5.4	7	1.7	3	6	0
#4	0.2	0.9	5.4	7	1.7	4.5	6	0.9–1.2 Sustained suction
#5	0.2	0.6	3.6	13.12	1.7	3	6	Manufacturing failure (internal-channel blockage)

In Variation #3, the 0.3 mm nozzle still failed to draw in any secondary flow, indicating that the jet momentum and pressure drop in the suction region were insufficient even though $D_m/D_n = 3$ and $L_m/D_m = 6$ fall within the recommended range. When the nozzle diameter was further reduced to 0.2 mm in Variation #4 ($D_m/D_n = 4.5$, $L_m/D_m = 6$), the ejector successfully entrained hydrogen from the recirculation line, achieving entrainment ratios in the range 0.9 – 1.2 . Because Variations #3 and #4 differ only in nozzle diameter, the contrasting results clearly show that nozzle diameters of 1 and 0.3 mm are too large for the low-capacity PEMFC considered here, whereas a 0.2 mm nozzle provides a sufficiently strong primary jet and favorable area ratio for effective entrainment. Reducing D_n from 0.3 to 0.2 mm increases the corresponding area ratio (A_m/A_n) from 9 to 20.25 , providing a

more favorable primary-jet/mixing-chamber relationship for the very low hydrogen-flow and pressure envelope of this stack. The superior performance of the 0.2 mm nozzle should therefore not be interpreted as evidence that further reduction in nozzle diameter would necessarily improve entrainment. A smaller nozzle would also increase pressure loss, restrict primary mass-flow capacity, and aggravate manufacturing tolerances. Thus, $D_n = 0.2$ mm represents the smallest successfully manufactured and experimentally viable nozzle within the investigated design space, rather than a universal optimum.

To address the need for theoretical and numerical validation of the ejector design, a steady, compressible, two-dimensional axisymmetric CFD model with a realizable k – ϵ turbulence closure was developed for the ejector geometry corresponding to Variation #4. The numerical model was validated against experimentally measured entrainment ratio, reproducing the measured value with discrepancies below approximately 3%, which confirms that the model captures both the magnitude and the trend of the ejector's entrainment behavior.

To further assess the rationality of the design, internal flow-field diagnostics were extracted from the validated CFD model, as shown in Figure 3. Figure 3(a) shows the static pressure distribution in the baseline ejector, while Figure 3(b) presents the corresponding velocity magnitude field. The primary channel on the left is characterized by a high static pressure, which is released as the flow is accelerated through the converging nozzle toward the throat. Immediately downstream of the nozzle exit, a high-speed jet forms, accompanied by a marked local drop in static pressure that extends into the entrance of the mixing section. This low-pressure core, together with the lower-pressure reservoirs in the upper and lower side branches, promotes the induction of the secondary flow from the suction ports. Along the mixing duct, the primary jet spreads and entrains the secondary stream, leading to a gradual equalization of both pressure and velocity across the cross-section. In the downstream diffuser, the jet decelerates, and the static pressure recovers toward the outlet value. The velocity contours do not indicate any extended separated regions in the diffuser; any recirculation, if present, appears confined to small corner zones, suggesting that the combination of mixing length and diffuser angle provides a reasonably effective pressure-recovery process. This indicates that the combination of mixing length and diffuser angle provides a reasonably effective compromise between entrainment capability and pressure recovery.

The volumetric flow-rate data used to calculate the entrainment ratio were extracted immediately before each purge event to minimize the influence of purge transients on the reported flow split. In the tested stack/controller, anode purging was governed by a fixed periodic strategy: the purge valve opened every 10 s for approximately 0.5 s. These purge settings (interval and duration) were kept identical for all load conditions and were the same for the dead-ended baseline as well as both recirculation modes. Consequently, the purge duty cycle was approximately constant ($\approx 5\%$), and the total purge-on time within each 5 min reporting window was effectively the same across operating points. Under this fixed-frequency purge logic, the total purged volume was observed to be nearly constant across the tested loads, because the controller did not adapt purge frequency or duration with current for the employed stack.

It should be noted that the entrainment ratio reported here represents a pre-purge snapshot and may differ from a cycle-averaged entrainment ratio over the full purge cycle. The chosen timing reduces the immediate impact of purge-induced disturbances on the flow measurements; however, reporting cycle-averaged entrainment and/or event-resolved entrainment dynamics is recommended for future studies, particularly for sub-kW stacks where purge-driven effects can be prominent.

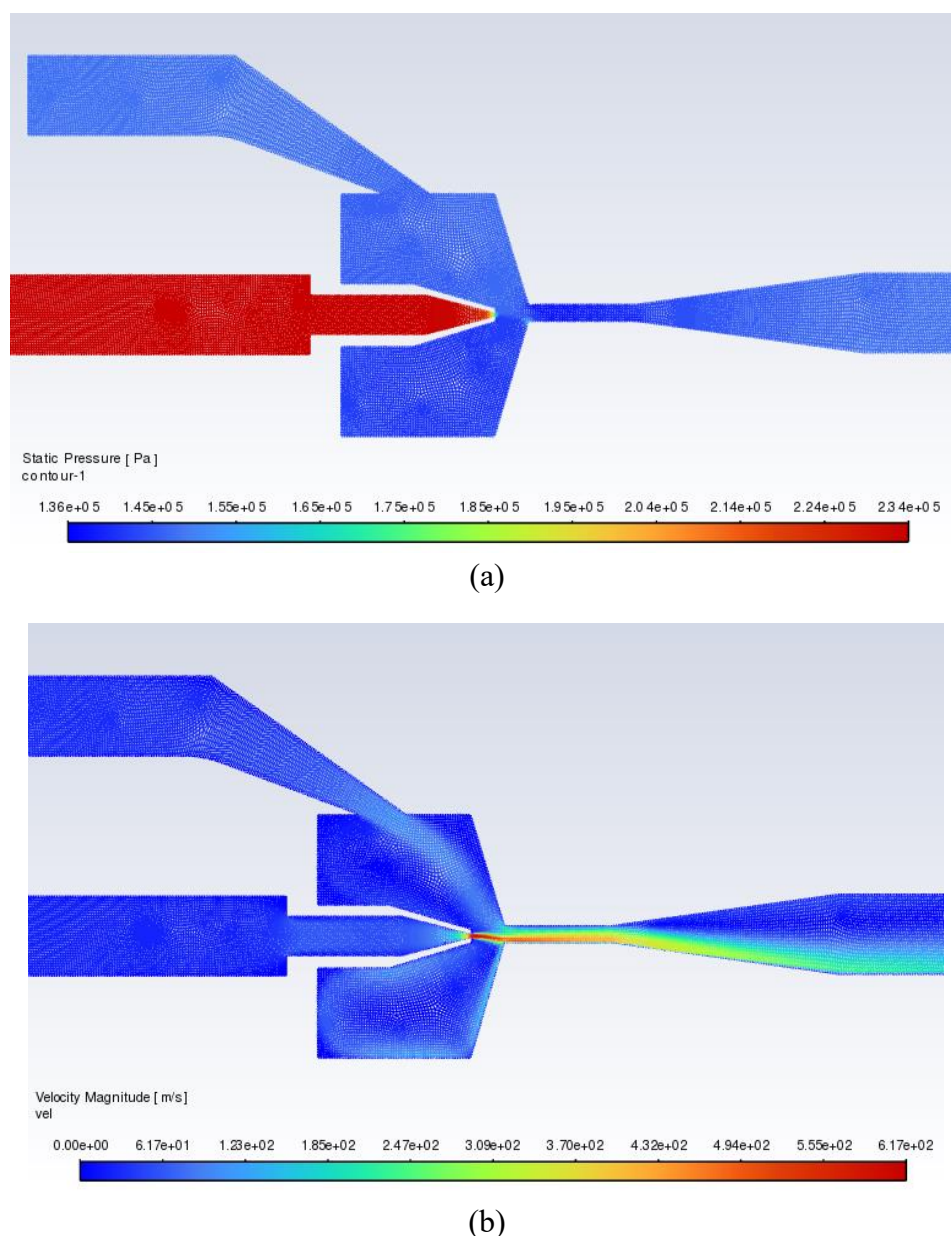


Figure 3. CFD-predicted internal flow field of the Variation #4 geometry: (a) static pressure contour; (b) corresponding velocity magnitude field.

The numerical values are plotted in Figure 4 for load currents between 0 and 20 A. The measured primary-flow characteristics increase approximately linearly with current and are consistent with trends reported in previous numerical studies [5,16]. In contrast, the secondary-flow behavior shows stronger load dependence: the difference between secondary and primary flow rates decreases as the load increases, and at high currents (above ~ 14 A), the secondary flow becomes slightly lower than the primary flow. As a result, the entrainment ratio increases rapidly from near zero at 0 A to a peak value of approximately 1.3 at approximately 2 A, then gradually decreases and approaches a quasi-steady range of 0.9–1.0 for currents above approximately 10 A. Across the non-zero operating range, the ejector maintains sustained suction (i.e., $\varepsilon_{\text{tot}} > 0$), and ε_{tot} remains above 0.2 at all operating points except 0 A. This value is often cited in the literature as a practical operability indicator for ejector suction [17]; however, it should not be interpreted as a direct criterion of hydrogen

recirculation effectiveness because the secondary stream is not necessarily pure hydrogen.

Because the secondary stream is not species-resolved, Figure 4 reports ε_{tot} as a total-stream entrainment metric. The corresponding effective hydrogen entrainment is reported as the bounded quantity ε_{H_2} using the composition-sensitivity framework defined in Section 3.2. The three ε_{H_2} curves (dashed lines) consistently lie below ε_{tot} , with $x = 0.15$ yielding the greatest reduction—a result that is physically intuitive, as a higher fraction of non-hydrogen species (water vapor/inert gases) in the secondary stream results in a lower effective entrainment ratio for pure hydrogen.

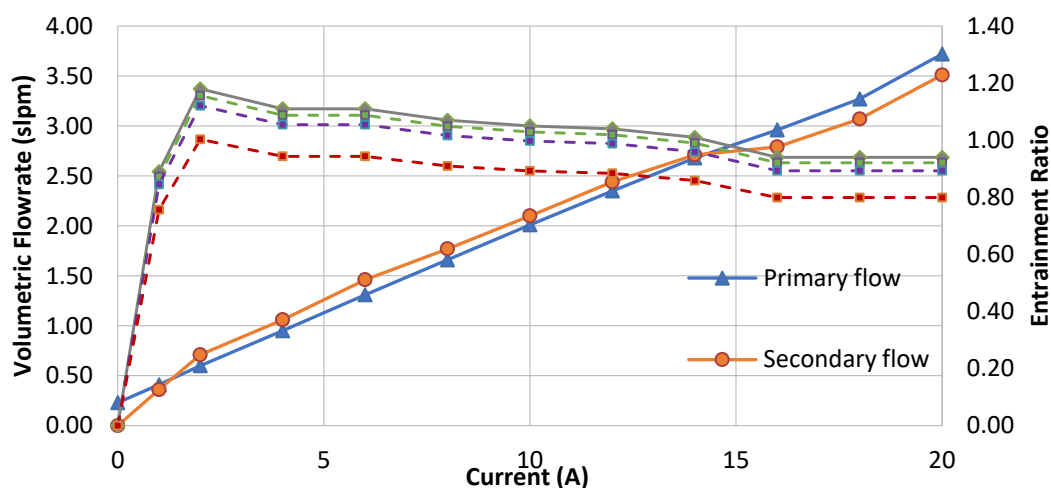


Figure 4. Volumetric flow rate and entrainment ratio of the resulting ejector.

4.2. Hydrogen recirculation system performance

The final ejector specifications (Variation #4) are listed in Table 3. Using this ejector, experiments were conducted on the 0.5-kW open-cathode stack for the two recirculation modes defined in the Introduction. Rec0 uses a fixed supply/regulator setting without intentional shaping of the anode inlet-pressure trajectory. In RecP, the same recirculation hardware and purge schedule are retained, while the supply regulator is manually adjusted to keep the anode inlet pressure near 0.5 barg during the measurement window. Thus, the intended difference between Rec0 and RecP is the inlet-pressure trajectory, not the ejector hardware or purge logic.

Table 3. Specifications of the resulting ejector.

Parameter	Value
Operating temperature	27 °C
Minimum primary pressure	0.6 barg
Maximum primary pressure	3 barg
Nozzle diameter	0.2 mm
Maximum suction flow	1.8 slpm
Ejector material	Resin
Primary port outer diameter	6 mm
Secondary port outer diameter	6 mm
Output port outer diameter	6 mm

The RecP (manual pressure-set) operation was implemented by temporarily increasing the tank-regulator pressure immediately after each purge to recover anode inlet pressure, then reducing the regulator setting as the inlet pressure approached 0.5 barg. This adjustment was repeated during each 5 min reporting period. Because RecP intentionally changes the supply-pressure trajectory, the comparison with the original dead-ended baseline is an operating-mode comparison rather than a strictly pressure-matched isolation of the recirculation effect. Any additional RecP saving relative to the dead-ended baseline may therefore contain contributions from both passive recirculation and pressure-profile shaping. A dead-ended run with a matched inlet-pressure trajectory is required to isolate these effects rigorously.

The 5 min integrated results are summarized in Table 4. The hydrogen-feed volume reduction reported in Table 4 is calculated directly from the measured fresh-hydrogen supply volumes and is therefore independent of the assumed $\text{H}_2/\text{N}_2/\text{H}_2\text{O}$ composition of the anode exhaust. The exhaust-composition uncertainty affects the derived hydrogen-utilization and effective hydrogen-entrainment metrics, but not the measured fresh-hydrogen feed reduction itself. The recirculation modes require less fresh hydrogen than the dead-ended baseline at all tested loads, with the largest relative differences at low load. Because RecP also modifies the inlet-pressure trajectory, Table 4 should be interpreted as a comparison among operating strategies; the RecP–dead-ended difference cannot be attributed exclusively to recirculation hardware.

Table 4. Hydrogen-feed volume reduction relative to the dead-ended baseline (%).

Load (A)	Hydrogen feed (mL)			Volume reduction	
	Dead-ended	Rec0	RecP	Rec0	RecP
3	5107.87	4552.54	4279.66	11%	16%
6	7586.54	7043.03	6750.37	7%	11%
9	10,026.86	9499.76	9215.73	5%	8%
12	12,449.98	11,945.57	11,688.03	4%	6%
15	14,772.55	14,238.88	13,978.41	4%	5%
18	16,926.57	16,339.32	16,145.01	3%	5%

As expected, hydrogen feed increases with load current due to the higher stoichiometric demand at higher currents. Nevertheless, at every operating point, the recirculation modes require less hydrogen than the dead-ended baseline. The relative reduction is most pronounced at low load (e.g., approximately 11% for Rec0 and 16% for RecP at 3 A) and decreases toward high load (approximately 3% and 5%, respectively, at 18 A). This trend is consistent with the system operating in a purge-influenced regime at low current, where purge-related transients contribute a larger fraction of the measured supply flow, and with the buffering role of recirculation in mitigating post-purge recovery demands. However, because Table 4 is based on 5-min integrated volumes, attributing the savings to a specific mechanism (e.g., overshoot suppression) requires event-based characterization.

In the present stack/controller, purge timing was fixed (10 s interval and ~0.5 s duration) across all loads and modes, so purge scheduling itself did not scale with current. This explains why the total purge-on time (and the associated purged volume) remained similar across operating points and implies that the observed differences in integrated hydrogen feed primarily reflect the presence of recirculation and, for RecP, the imposed inlet-pressure trajectory. At higher currents, the purge-induced transient contribution becomes smaller relative to the total hydrogen demand, which naturally reduces

the marginal feed savings attributable to recirculation. Finally, RecP exhibits higher hydrogen utilization than Rec0, indicating that maintaining a prescribed anode inlet pressure can further improve steady utilization beyond passive recirculation alone, even though with a trade-off in transient suppression as discussed in the purge-event analysis.

Figure 5(a) and (b) compares the nominal/pure hydrogen-utilization values $\eta_{H_2,0}$ and gross fuel-to-stack electrical efficiency η_{FC} for the dead-ended baseline and the two recirculation modes. The utilization curves converge at high current, while the efficiency curves exhibit a load-dependent crossover: RecP is relatively favorable at low-to-medium current, whereas Rec0 becomes marginally more efficient at the highest loads. This behavior follows directly from the definition of η_{FC} , which depends on both the electrical energy produced and the measured fresh-hydrogen feed. Although RecP consistently reduces hydrogen feed relative to Rec0, its additional feed-saving advantage decreases with increasing current. Consequently, at high load, relatively small differences in stack voltage and electrical-energy production can outweigh the remaining hydrogen-feed advantage of RecP. Because RecP involves manual shaping of the inlet-pressure trajectory, the present data do not allow this crossover to be attributed to a single physical mechanism. The result is therefore interpreted as a load-dependent trade-off between hydrogen-feed reduction and electrical output rather than a monotonic efficiency benefit of pressure regulation.

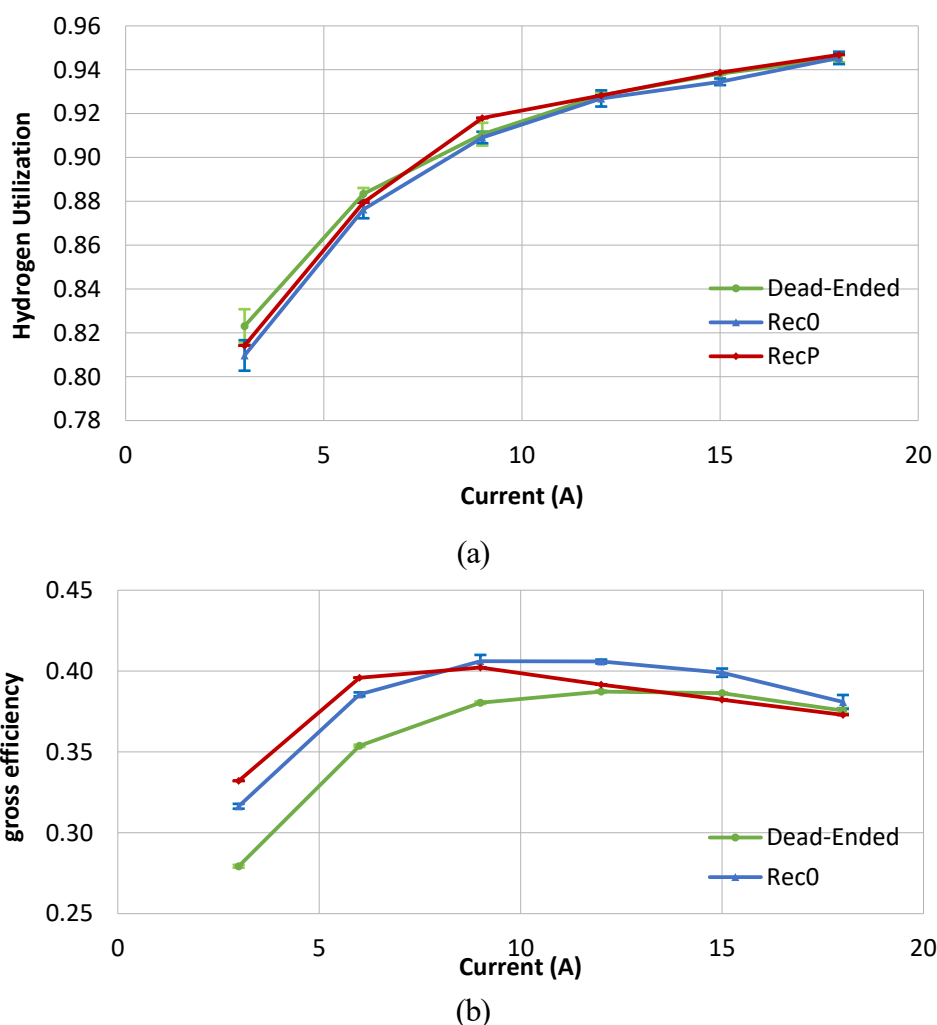


Figure 5. Comparison among dead-ended, Rec0, and RecP operation: (a) pure hydrogen-utilization; (b) gross fuel-to-stack electrical efficiency.

To quantify this trade-off, a trade-off index TI is defined as the ratio of the relative gain in steady-state hydrogen utilization to the relative increase in normalized purge-excess volume when switching from Rec0 to RecP. At 3 A, RecP yields only a 0.56% relative utilization gain over Rec0 at the cost of a 220% increase in normalized purge-excess volume, giving $TI \approx 0.0025$. At 18 A, the utilization gain narrows further to 0.15%, while the transient-severity increase grows to roughly 1990%, giving $TI \approx 0.00007$ —a further 35-fold reduction. This confirms quantitatively that RecP's utilization benefit is disproportionately small relative to its transient-suppression penalty, and that this imbalance worsens with load, consistent with the efficiency crossover reported in Figure 5(b).

The sensitivity analysis described in Eq (2) indicates that the influence of the assumed exhaust composition on hydrogen utilization is relatively small. Over the investigated operating range, the largest composition-induced shift occurs for the most conservative case of $x = 15\%$, for which the calculated hydrogen utilization increases by a maximum of approximately 3.8% relative to the nominal value $\eta_{H_2,0}$. Smaller assumed non-hydrogen fractions of 2% and 5% produce correspondingly smaller shifts. Therefore, although the exhaust-composition assumption affects the absolute value of η_{H_2} , it does not alter the comparative trends among the tested operating modes.

Figure 6 compares the I–V characteristics of the stack under dead-ended, Rec0, and RecP operation. As expected, the stack voltage decreases with increasing current for all three operating modes. The dead-ended and Rec0 curves remain relatively close over most of the investigated current range, indicating that passive recirculation without an imposed inlet-pressure setpoint introduces only a small change in the stack-voltage response. In contrast, RecP exhibits a progressively lower stack voltage at medium-to-high currents, with the separation becoming more pronounced above approximately 9–12 A.

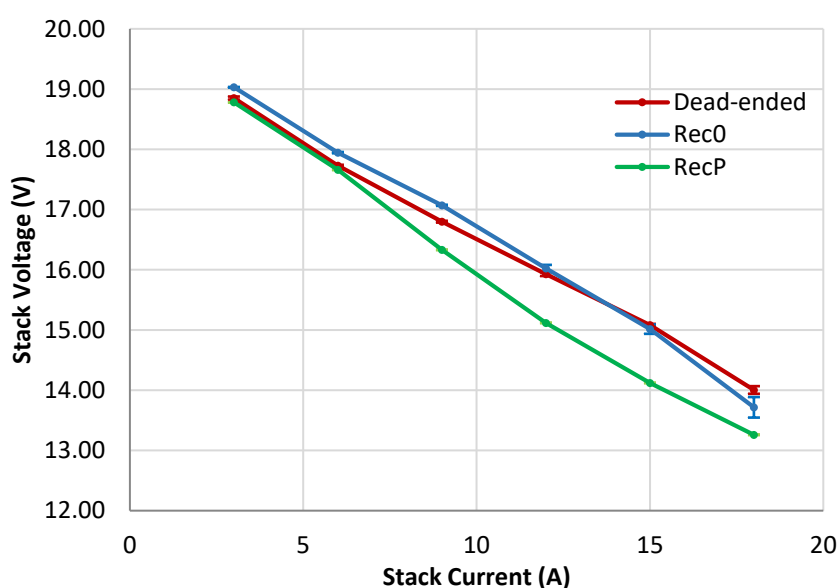


Figure 6. I–V characteristics of the open-cathode PEMFC under dead-ended, Rec0, and RecP operating modes.

This load-dependent voltage behavior helps explain the system-efficiency trends in Figure 5(b). At low current, the additional reduction in fresh-hydrogen feed achieved by RecP is sufficiently large to compensate for its small voltage difference relative to Rec0. However, the additional hydrogen-feed benefit of RecP decreases as the load increases, whereas the voltage difference remains appreciable. Consequently, at high current, the reduction in electrical energy output can outweigh the remaining

fresh-hydrogen saving, resulting in a lower feed-based efficiency for RecP than for Rec0. The observed voltage difference is therefore interpreted as a load-dependent effect associated with the imposed inlet-pressure trajectory; the present measurements do not permit attribution to a specific electrochemical mechanism.

While Figures 5 and 6 summarize steady and integrated performance over the 5 min reporting window, these averages can mask the contribution of purge-driven transients that are particularly prominent in sub-kW open-cathode operation. Because purging is periodic and fixed in the tested stack/controller, a time-resolved analysis is required to separate purge-event dynamics from quasi-steady entrainment behavior. Therefore, this work next quantifies purge-event transients using time-series traces and event-based metrics.

5. Discussion

5.1. Purge-event transient analysis and metrics

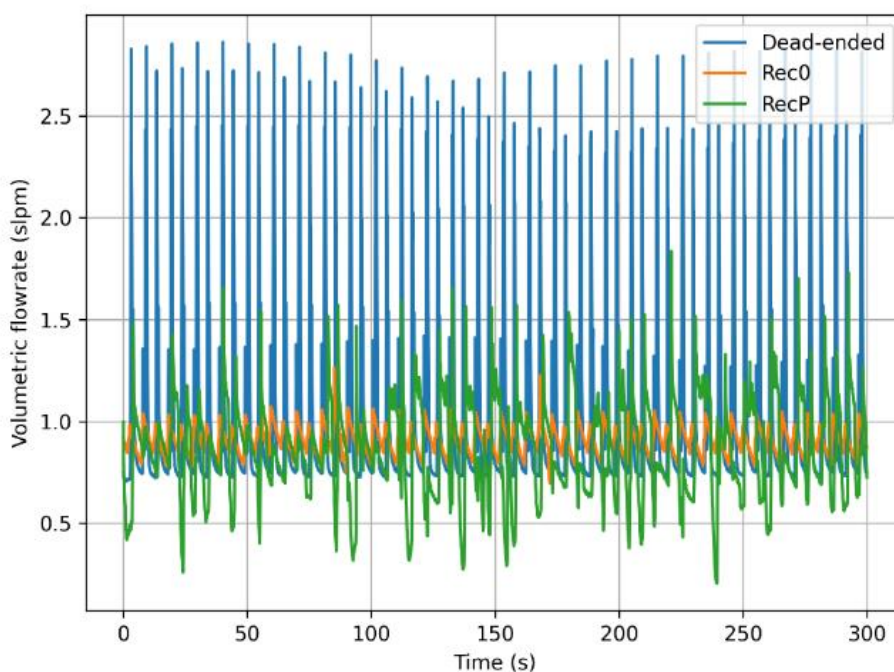
The purge-transient metrics defined in Section 2.3 were evaluated at 3 and 18 A. Values are obtained from event-by-event analysis of approximately 30 purge events per 300s run and are summarized across three independent experimental runs. Purge events within the same run are treated as repeated observations rather than independent experimental replicates. Table 5 summarizes the dimensional and normalized purge-response metrics, while Figures 7 and 8 show the corresponding time-resolved supply-flow responses. Across both loads, the normalized metrics reveal a clear ordering among the three operating modes on average: Rec0 consistently exhibits the smallest overshoot and excess volume, RecP is intermediate, and the dead-ended baseline shows by far the largest values. At 3 A, the dead-ended condition produces a normalized overshoot of 6.7206 and a normalized excess volume of 0.3519, meaning the purge-induced excess flow delivered in a single purge event is more than a third of the baseline flow sustained over one full purge-cycle interval. Rec0 reduces this while RecP falls in between, indicating that ejector-driven recirculation without an imposed pressure setpoint buffers purge excursions more effectively than the manually shaped pressure trajectory of RecP. The buffering effect becomes even more pronounced at 18 A, in which the purge event becomes almost undetectable relative to the elevated baseline recirculation flow.

Table 5. Purge-transient metrics at 3 and 18 A for the three operating modes.

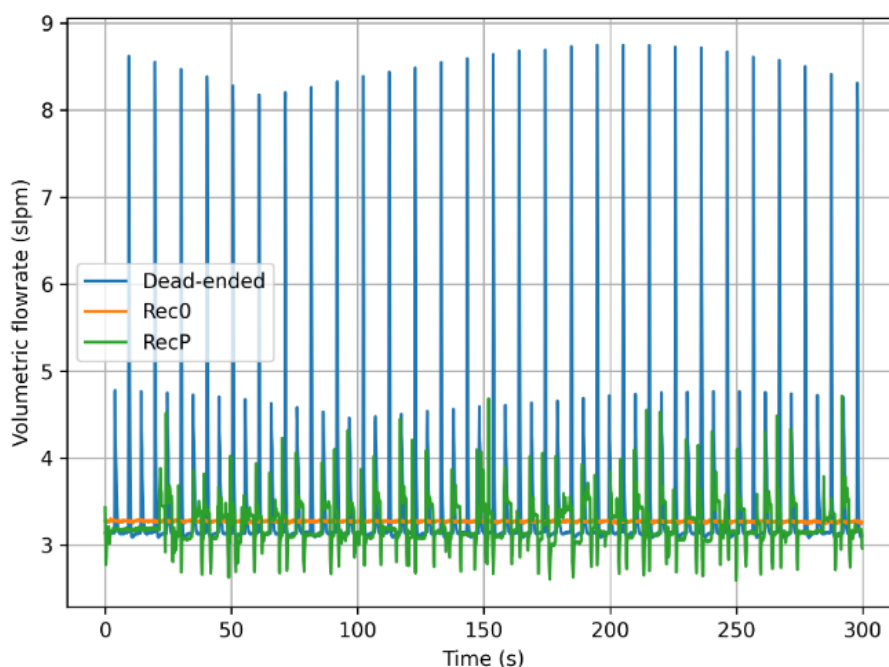
Load	Mode	q_b (slpm)	A_{os} (slpm)		A_{os}^*	V_{ex} (mL/purge)		V_{ex}^*
			mean	SD		mean	SD	
3 A	Dead-ended	0.7652	5.1427	0.4161	6.7206	44.88	4.13	0.3519
	Rec0	0.9056	0.1822	0.0905	0.2012	5.53	1.46	0.0366
	RecP	0.8487	0.6512	0.2218	0.7672	16.58	7.14	0.1172
18 A	Dead-ended	3.1376	5.7035	0.182	1.8178	42.73	3.86	0.0817
	Rec0	3.2684	0.0468	0.0076	0.0143	0.91	0.27	0.0017
	RecP	3.1737	1.0333	0.3743	0.3256	18.83	6.5	0.0356

Notably, the absolute excess volume V_{ex} does not follow a strictly monotonic trend with load for every mode: it decreases substantially for Rec0 (mean reduction 5.53 to 0.91 mL/purge) but changes only slightly for the dead-ended case (mean reduction 44.88 to 42.73 mL/purge) and even increases

modestly for RecP (mean reduction 16.58 to 18.83 mL/purge). This asymmetry suggests that the effectiveness of ejector recirculation in absorbing purge-induced flow spikes scales favorably with load, whereas RecP's manually imposed pressure trajectory does not confer the same load-dependent benefit—consistent with the crossover behavior observed in the system-efficiency results (Figure 5b), where RecP's advantage over Rec0 diminishes and eventually reverses at high current.

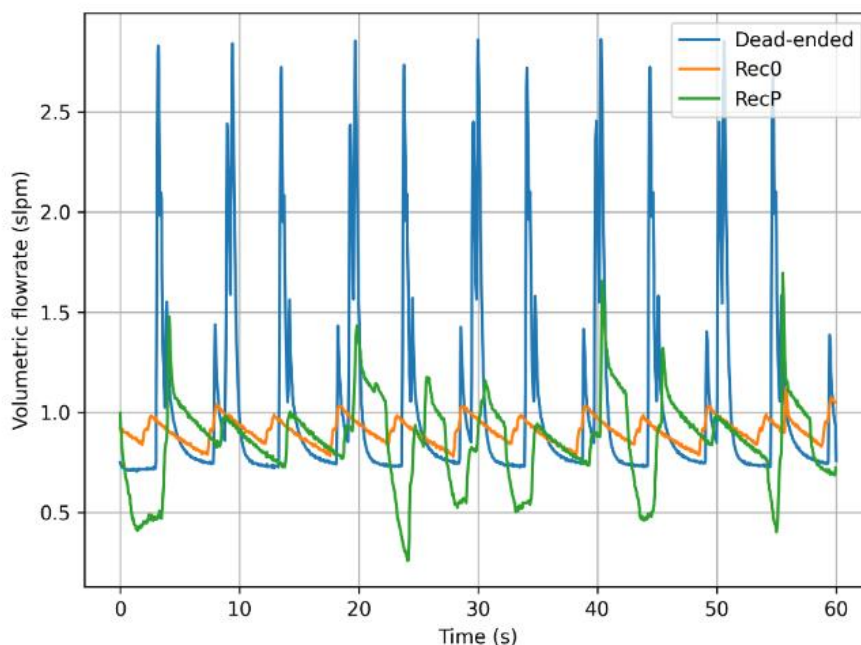


(a)

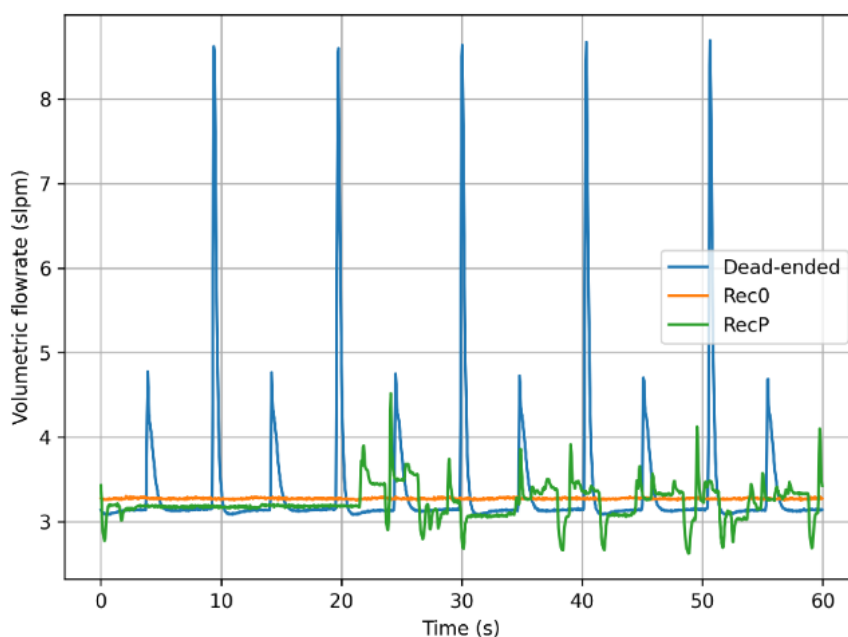


(b)

Figure 7. Time-resolved hydrogen supply flow for dead-ended, Rec0, and RecP operation at (a) 3 A and (b) 18 A.



(a)



(b)

Figure 8. Zoomed view (0–60 s) of the hydrogen-supply-flow time series for dead-ended, Rec0, and RecP operation (representative run): (a) 3 A; (b) 18 A.

The zoomed view (Figure 8) highlights immediate post-purge recovery behavior and reveals a clear buffering effect when the recirculation loop is active. Compared with dead-ended operation, both recirculation modes reduce purge-induced spikes and smooth recovery; Rec0 shows the strongest attenuation, whereas RecP provides partial suppression. Quantitatively, Rec0 reduces overshoot amplitude and purge-induced excess per event by approximately 90% and 88% at 3 A, and by approximately 99% and 98% at 18 A. Because purge interval and duration are identical across all loads

and modes (10 s and ~ 0.5 s), these reductions are attributable to the operating mode rather than changes in purge scheduling. The stronger suppression in Rec0 is consistent with a less constrained inlet-pressure trajectory during purge recovery, allowing the recirculation loop to replenish anode inventory with reduced reliance on rapid make-up draw from the supply. In contrast, RecP intentionally shapes the inlet-pressure trajectory toward a fixed target (0.5 barg), which can increase replenishment demand immediately after purge events and thereby partially offset the transient buffering benefit, even while improving steady utilization.

The purge-event results demonstrate that the system-level benefit of passive recirculation is not limited to steady entrainment but also includes transient buffering during purge recovery. This motivates a broader discussion on why sub-kW open-cathode stacks differ from the medium/high-power regimes commonly studied in the ejector literature. Building on these findings, we summarize practical implications and provide design- and operation-oriented guidance for small-scale systems.

5.2. Implications for sub-kW operation

In sub-kW open-cathode PEMFCs, limited pressure/flow margins and fixed periodic purging can dominate measured hydrogen withdrawal, requiring evaluation and control strategies that explicitly account for purge-event dynamics. For the present ejector–stack combination, approximately 2 A marks the current at which suction becomes firmly established, whereas approximately 10 A marks the onset of the quasi-asymptotic entrainment region. These values should be interpreted as empirical transition points specific to the tested ejector geometry and stack operating envelope, rather than as universal design thresholds for sub-kW PEMFC systems. Although the qualitative entrainment trends align with prior ejector literature, the present study adds quantitative insight for the purge-influenced, sub-kW open-cathode regime. Under fixed periodic purging, purge-on time (and, therefore, purge-related discharge) is only weakly dependent on load, so system-level hydrogen savings depend strongly on (i) the effective hydrogen recirculation captured by a hydrogen-entrainment bound and (ii) the inlet-pressure trajectory imposed by the supply regulator. Based on the results, the following practice-oriented guidance is suggested for small open-cathode stacks: (1) report both ε_{tot} and bounded ε_{H_2} to reflect composition uncertainty in the secondary stream; (2) complement integrated 5-min consumption with purge-event metrics (overshoot amplitude and purge-induced excess volume per event) to capture purge-dominated behavior; (3) when comparing operating strategies that shape inlet pressure, use matched-pressure baselines to separate the effects of pressure trajectory from recirculation hardware; and (4) interpret small differences in η_{H_2} and η_{FC} with bounded uncertainty rather than single-point estimates.

More broadly, the dominant constraints in sub-kW open-cathode stacks differ from those in medium-to-high-power (often closed-cathode) PEMFC systems, which can change both the interpretation of entrainment and the system-level value proposition of passive recirculation. Sub-kW stacks operate with low absolute anode pressures and low hydrogen flow rates; in this regime, ejector performance can be highly sensitive to small backpressure variations and inlet-pressure trajectory. In addition, purge strategies in many small commercial stacks are periodic and fixed, so purge-driven disturbances can contribute a comparatively large fraction of the measured cylinder withdrawal, producing pronounced transient spikes that are difficult to separate from steady entrainment if only integrated averages are reported. Composition effects further amplify uncertainty: nitrogen crossover and water vapor in the anode exhaust can bias utilization estimates when the exhaust is treated as

nearly pure hydrogen. Finally, micro-ejector fabrication (e.g., $D_n \approx 0.2$ mm) can be sensitive to manufacturing tolerances (area scales with d^2), motivating bounded reporting and uncertainty discussion. Taken together, these factors suggest that sub-kW open-cathode systems require evaluation approaches that explicitly consider purge-event dynamics, pressure-trajectory constraints, and composition-driven uncertainty, rather than relying solely on steady entrainment metrics.

The present experiments were designed to assess ejector operability, hydrogen use, and short-term purge dynamics rather than long-term degradation; therefore, no direct durability improvement is claimed. Suppression of purge-induced supply excursions may reduce exposure to transient hydrogen-starvation conditions, but this potential benefit must be balanced against possible inert-gas/water accumulation and pressure cycling. Dedicated long-duration tests should quantify voltage-degradation rate, resistance growth, and ejector material stability under matched dead-ended, Rec0, and purge-aware pressure-controlled operation. Transient behavior can also arise from thermal start-up/shut-down dynamics. Recent work on a metal-foam SOFC stack demonstrates the importance of thermal-field design for rapid and safe thermal cycling [29], although its high-temperature time scales differ substantially from the purge-induced anode-flow transients studied here. Future open-cathode PEMFC work should therefore examine the interaction of purge events with load transitions and start-up/shut-down thermal dynamics.

For sub-kW open-cathode PEMFC systems, the results indicate that the implementation of passive ejector recirculation should be purge-aware. A practical recommendation is to operate with a moderate and stable anode inlet pressure sufficient to maintain robust entrainment and avoid starvation, while avoiding overly aggressive recovery to a fixed pressure target immediately after purge events, which can increase make-up demand and partially negate transient buffering. Consequently, a hybrid operating philosophy is suggested: pressure regulation may be used to improve steady utilization, but it should be tuned (or temporarily relaxed) during and shortly after purge windows to allow smooth inventory recovery through the recirculation loop. Future designs and controllers should therefore be evaluated using both steady and transient metrics, e.g., net hydrogen usage/utilization together with purge-induced excess volume or overshoot amplitude, to ensure that improvements in averaged performance do not come at the cost of increased purge transients.

6. Conclusions

This study experimentally investigated an ejector-based passive hydrogen recirculation system for a 0.5 kW small-scale open-cathode PEM fuel cell stack. Among the tested configurations, the $D_n = 0.2$ mm nozzle combined with $D_m/D_n = 4.5$ was the only configuration that achieved sustained suction over the investigated operating range. This result represents a feasible design within the tested low-flow/low-pressure envelope rather than a universal geometric optimum. Relative to the tested dead-ended operating baseline, the recirculation operating modes reduced integrated fresh-hydrogen feed, with the largest relative differences occurring at low current.

A key finding is that, in this sub-kW open-cathode system with fixed purge timing (10-s interval, ~ 0.5 -s duration), hydrogen savings are strongly influenced by purge-event dynamics. Time-resolved analysis shows that, despite event-to-event variability, both recirculation modes consistently reduced purge-induced supply-flow excursions relative to the dead-ended configuration, with Rec0 showing the strongest suppression on average. These observations support the interpretation that a portion of the measured feed reduction is associated with reduced post-purge

overshoot and purge-induced excess withdrawal. The comparison between Rec0 and RecP further indicates that shaping the anode inlet-pressure trajectory can improve steady utilization, but may partially compromise transient suppression, motivating purge-aware pressure regulation for small-scale systems.

Overall, the results support the technical feasibility of compact passive recirculation for the tested sub-kW open-cathode stack while also defining important limitations. Composition-resolved measurements are needed to reduce uncertainty in hydrogen utilization and effective hydrogen entrainment; pressure-matched dead-ended tests are needed to isolate the hardware recirculation effect from pressure-trajectory shaping; and event-level statistical analysis is required for robust purge-transient inference. Future work should additionally validate the micro-ejector flow field using CFD and/or direct pressure measurements, implement closed-loop purge-aware pressure control, and perform long-duration durability testing. Reporting both steady and normalized transient metrics is recommended so that averaged hydrogen-feed savings are not interpreted without the associated purge-recovery dynamics.

Use of AI tools declaration

During the preparation of this manuscript, the authors used generative artificial intelligence tools to improve the readability and language of the text, including grammar correction, sentence refinement, and paraphrasing. All generated output was reviewed, revised, and validated by the authors, who take full responsibility for the final content of this manuscript.

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Conflict of interest

The authors declare no conflicts of interest.

Author contributions

Katherin Indriawati: conceptualization, methodology, writing an original draft, funding acquisition; Naufal Ikhsan Wijaya: project administration, investigation, visualization; Edy Herianto Majlan: review & editing, formal analysis.

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