WHITEPAPER

Axial-flux permanent-magnet machines for direct-drive lift rotors: sizing, thermal limits and failure behaviour

A literature-based review of how axial-flux permanent-magnet machines are sized for direct-drive lift rotors: hover torque requirement, torque-sizing relations, design and optimisation methods, reported parameter ranges, fault behaviour and regulatory context.

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Abstract

Distributed-lift aircraft use many fixed-pitch rotors that operate in hover at low angular speed and high torque. This paper reviews the engineering relations that connect rotor thrust to the torque and thermal capability required from an axial-flux permanent-magnet (AFPM) machine driving the rotor directly. Starting from momentum theory, it derives the hover shaft torque for a generic eight-rotor configuration, then examines the torque-sizing relation of AFPM machines reported in the literature, the geometric ratio that governs it, and the conditions under which an AFPM machine is predicted to out-torque a radial-flux machine of the same outer radius. It summarises design and optimisation methods (analytical, magnetic-equivalent-circuit with thermal network, three-dimensional finite-element analysis and surrogate-assisted optimisation), tabulates ranges reported in peer-reviewed sources, and reviews inter-turn short-circuit and demagnetisation behaviour and the regulatory text that frames single-failure tolerance. The sources report model results and prototype measurements at different scales; the paper distinguishes them and states the limits of each. Open problems concern validated thermal models at the continuous-duty point, post-fault behaviour of permanent-magnet machines at aircraft power levels, and the lack of public measured data at rotor-drive scale.

1. Introduction and scope

A lift rotor in hover absorbs a power fixed by its thrust and disk area, at an angular speed limited by blade tip speed. For rotors of 1–3 m diameter this speed lies in the low thousands of revolutions per minute, so the torque at the shaft is large compared with the torque of high-speed machines of equal power. A machine that produces high torque in a short axial length is therefore attractive, and the axial-flux permanent-magnet machine, in which the airgap flux runs parallel to the shaft, is the topology most often proposed for this duty [2][3].

The scope of this paper is the electromagnetic and thermal sizing of the machine, its failure behaviour, and the regulatory frame. It does not treat rotor aerodynamics beyond the momentum relations needed to obtain the load torque, nor inverter design, nor propeller structure. Statements are attributed to the source that reports them; values calculated here from stated assumptions are labelled as such. For a general introduction to the product category, see the site’s electric motors guide.

2. Theory and governing equations

2.1 Hover power and shaft torque

For a rotor of disk area A producing thrust T in hover, momentum theory gives the ideal induced power

$$P_{\text{ideal}} = \frac{T^{3/2}}{\sqrt{2\rho A}}$$

where T is thrust (N), ρ is air density (kg/m³) and $A = \pi D^2/4$ is the disk area (m²) for rotor diameter D (m). The shaft power is $P = P_{\text{ideal}}/FM$, where FM is the figure of merit (dimensionless), the ratio of ideal to actual hover power. In non-dimensional form, with thrust coefficient $C_T = T/[\rho A(\Omega R)^2]$ and power coefficient $C_P = P/[\rho A(\Omega R)^3]$, the figure of merit is $FM = C_T^{3/2}/(\sqrt{2}\,C_P)$, as used in the NASA and U.S. Army analysis of hover efficiency limits [1]. Here Ω is the rotor angular speed (rad/s) and $R = D/2$ the rotor radius (m). The same analysis notes that comparing figures of merit across different disk loadings can be misleading, and expresses efficiency potential as power loading (T/P) instead [1].

With a tip-speed limit V_{tip}, the angular speed is $\Omega = V_{tip}/R$ and the shaft torque is

$$Q = \frac{P}{\Omega} = \frac{T^{3/2}}{\Omega\, FM\, \sqrt{2\rho A}}$$

where Q is in N·m. Two consequences follow directly. Power varies as $T^{3/2}$, so any redistribution of thrust onto fewer rotors raises their power more than proportionally. And, at fixed tip speed, a larger rotor needs less power but turns more slowly, so Q falls more slowly than P.

Worked example (generic, illustrative assumptions). An aircraft of 1,500 kg is carried by eight identical rotors, so $T = 1500 \times 9.81/8 = 1{,}839$ N per rotor. With $D = 2.5$ m ($A = 4.91$ m², disk loading 375 N/m²) and $\rho = 1.225$ kg/m³, $P_{\text{ideal}} = 1839^{3/2}/\sqrt{2 \times 1.225 \times 4.91} = 22.7$ kW. Assuming $FM = 0.75$, $P = 30.3$ kW. For an assumed tip speed of 200 m/s, $\Omega = 200/1.25 = 160$ rad/s (1,528 rpm) and $Q = 30{,}330/160 = 190$ N·m. If one rotor is lost and the other seven share the thrust, $T' = 1839 \times 8/7 = 2{,}102$ N, $P' = 30.3 \times (8/7)^{3/2} = 37.1$ kW and $Q' = 232$ N·m at the same speed. This neglects the additional thrust needed for yaw and roll control, so Q' is a lower bound on the failure case. The machine's requirement is thus a continuous torque near 190 N·m and a short-duration torque of at least 232 N·m, at about 1,500 rpm, where the mechanical power is only 30–37 kW.

2.2 Torque-sizing relations

For a radial-flux permanent-magnet machine of airgap radius R_o and axial length L, the electromagnetic torque obtained by integrating the Maxwell stress over the airgap is [2]

$$T_{RF} = \pi R_o^2 L\, \hat{B}_\delta \hat{A}$$

where $\hat{B}_\delta$ is the peak of the fundamental airgap flux density, the magnetic loading (T), and $\hat{A}$ is the peak linear current density, the electric loading (A/m). An axial-flux machine has no single airgap radius: the airgap spans from an inner radius R_{im} to an outer radius R_{om}. Two sizing approaches are in use. One defines the electric loading at the average radius and expresses the power through an Essen-type coefficient, in which the radius ratio $\lambda = R_{im}/R_{om}$ is the only independent geometric term [2]. The other treats $\hat{A}$ as a function of radius, and for the yokeless-and-segmented-armature (YASA), coreless and single-sided configurations the review reports the closed form [2]

$$T_{AF} = \tfrac{\pi}{2}\,\hat{B}_\delta\,\hat{A}\,R_{om}^3\,\lambda\,(1-\lambda^2)$$

where T_{AF} is torque (N·m), R_{om} the outer active radius (m), λ dimensionless, and $\hat{A}$ is defined as in [2]. Dimensional check: T × A/m × m³ = (V·s/m²)(A/m)(m³) = V·A·s = N·m.

Three properties follow from the relation.

1. **Cubic dependence on radius.** At fixed loadings and λ, doubling R_{om} multiplies torque by 8 [2].
2. **Radius ratio.** The product $\lambda(1-\lambda^2)$ has its maximum, $2/(3\sqrt{3}) = 0.385$, at $\lambda = 1/\sqrt{3} = 0.577$ (set $d[\lambda(1-\lambda^2)]/d\lambda = 1 - 3\lambda^2 = 0$). The review reports that λ between 0.65 and 0.75 has been used in power-dense designs [2]. At $\lambda = 0.70$ the factor is 0.357, that is 93 % of the maximum, while the active annulus area, proportional to $1-\lambda^2$, is 0.51 of the outer disc instead of 0.67 (calculated here).
3. **Loadings are set by materials and heat.** $\hat{B}_\delta$ is limited by magnet properties and by saturation and core loss in the electrical steel; the maximum $\hat{A}$ is determined by thermal considerations for a given design [2].

Worked example (generic). To produce $Q' = 232$ N·m with assumed loadings $\hat{B}_\delta = 0.9$ T and $\hat{A} = 60$ kA/m at $\lambda = 0.70$, the relation requires $R_{om}^3 = 232/[(\pi/2)(0.9)(60{,}000)(0.357)] = 7.66 \times 10^{-3}$ m³, so $R_{om} = 0.197$ m, an active diameter of 0.39 m. Raising the electric loading to 100 kA/m, which requires a correspondingly better heat path, gives $R_{om} = 0.166$ m (diameter 0.33 m). Because torque scales with the cube of radius and linearly with $\hat{A}$, a 67 % increase in electric loading reduces the required radius by only 16 %. The loadings in this example are assumed for illustration; they are not measured values from [2].

2.3 Comparison with radial-flux machines

Dividing the two sizing relations gives the torque ratio of an AFPM and an RFPM machine of equal outer radius and equal loadings [2]:

$$\frac{T_{AF}}{T_{RF}} = \frac{\lambda(1-\lambda^2)\,R_{om}}{2L} = \frac{D_{om}\,\lambda(1-\lambda^2)}{4L}$$

where $D_{om} = 2R_{om}$ (m) and L is the axial length of the radial-flux rotor (m). The ratio exceeds 1 only for sufficiently flat machines: $D_{om}/L > 4/[\lambda(1-\lambda^2)]$, which equals 10.4 at $\lambda = 0.577$ and 11.2 at $\lambda = 0.70$ (calculated here). The review reports from its

contour plot that at radial-flux aspect ratios below about 12 the axial-flux machine of the same outer radius produces less torque, and above it more, under the assumption of equal electric loading; it adds that the AFPM structure is more conducive to cooling [2], which could allow a higher loading in practice [2].

A second figure of merit is torque per rotor volume (TRV). For the radial-flux machine $TRV_{RF} = \hat{B}_\delta \hat{A}$. For the AFPM, with rotor volume $\pi R_{om}^2 (1-\lambda^2)(t_m + t_y)$, where t_m and t_y are the magnet and rotor-yoke thicknesses (m), dividing T_{AF} by this volume gives

$$TRV_{AF} = \frac{\lambda R_{om}}{2(t_m + t_y)}\,\hat{B}_\delta \hat{A}$$

(derived here from the relations above). This exceeds TRV_{RF} when $t_m + t_y < \lambda R_{om}/2$, which the review states holds for most practical designs [2]. The same review reports that single-sided and YASA machines have the same torque capability for the same permanent-magnet volume, which implies roughly twice the torque per rotor volume for the single-sided arrangement [2].

2.4 Losses and thermal limit

The continuous rating of the machine is set by the temperatures of the winding, magnets and insulation, not by the magnetic circuit. The heat sources are winding copper loss, stator core loss, and eddy-current loss in magnets and conductors. Two measures reported for reducing the last are magnet segmentation and stator construction. In a 4 kW, 2,500 rpm YASA machine, using 14 cuboidal NdFeB segments per pole reduced eddy-current losses by a factor of 8 compared with unsegmented poles [2]. In a comparison of stator cores for a 14-pole, 12-slot machine, laminated steel was more efficient at low frequency, and laminated and soft-magnetic-composite cores had comparable efficiency at high frequency, which was attributed to the growth of eddy-current loss in the laminations with frequency [2]. Printed-circuit stators were reported to be more efficient and compact than laminated ones at equal output power but to be thermally limited in achievable current density [2]. Because the current density that the winding can carry is bounded by temperature, the continuous torque can be written, from the sizing relation, as proportional to the thermally admissible $\hat{A}$; the extent to which cooling raises that bound at a given scale must be established with a thermal model of the specific design (Section 3.3).

3. Design and sizing methods

3.1 Analytical and semi-analytical models

Analytical sizing gives the first estimate of R_{om}, λ and the loadings (Section 2.2). Two three-dimensional effects limit its accuracy: the radial variation of flux density caused by changes in slot, tooth and pole dimensions with radius, and flux fringing at the inner and outer radii [2]. Analytical and semi-analytical models that include these effects have been developed, and the review states that finite-element analysis (FEA) is the most accurate

technique for AFPM evaluation, with the cost that three-dimensional FEA is computationally heavy [2].

3.2 Optimisation with reduced three-dimensional evaluations

Two approaches reduce the cost of design optimisation. The first evaluates only a few designs by three-dimensional FEA and the rest by surrogate models. In the cited comparison, a two-level surrogate-assisted multi-objective differential evolution required 163 three-dimensional FEA evaluations to reach a Pareto front that conventional multi-objective differential evolution reached with 886 evaluations [2]. The second approach slices the machine into several two-dimensional computation planes. Among the three plane models compared in the literature, the linear-machine approximation required the least computation time, and 2D results agreed more closely with measurements for coreless designs than for iron-cored designs, which was attributed to the non-linearity of iron [2]. In one comparison for a 20 kW, 15,000 rpm coreless machine, 2D FEA with three planes reduced computation time significantly with a marginal loss of accuracy [2].

3.3 Coupled electromagnetic and thermal design for rotor drives

For lift-rotor duty, Arulampalam et al. coupled a magnetic equivalent circuit with a lumped-parameter thermal network and used it to minimise motor mass subject to the hover requirements of a research eVTOL vehicle, with a higher-fidelity finite-element model built for comparison [3]. They report a lightest design of 12 pole pairs and 27 slots with a mass of 9.28 kg, and that the analytical model under-predicted output power by approximately 9 % across several slot and pole-pair combinations relative to the finite-element model [3]. The abstract does not give the torque or efficiency values, so those cannot be cited here. The result is nevertheless informative for two reasons: the optimisation objective is mass at a thermally feasible hover point, which is the formulation implied by Section 2.1, and the authors identify weight and cooling as the two limits on high torque-to-weight axial-flux machines [3].

3.4 Stator and rotor construction

AFPM stators need radially oriented laminations, which are difficult to assemble, and this has been noted as one reason for limited commercial adoption [2]. Reported alternatives are tape-wound cores, soft magnetic composites, printed-circuit windings and additively manufactured cores; in one comparison the additively manufactured stator had lower core loss than laminated and composite stators at low and high (above 1 kHz) frequency [2]. For rotors, quasi-Halbach arrays were reported to give a 12 % increase in torque density in a torus machine compared with an axially magnetised array, and a sinusoidally shaped interior-magnet design was reported to be 35 % more torque dense with 60 % lower total harmonic distortion than a reference coreless machine [2]. Cogging torque was reduced by 73 % with alternating pole-arc ratios of 0.61 and 0.81 and by 48 % with a one-pole-pitch skew, relative to a pole-arc ratio of 0.81 [2]. These are simulation or prototype results at differing scales and are not directly comparable with each other.

4. Parameters and typical ranges

Table 1 collects ranges and values reported in the sources reviewed. The sources differ in scale, topology and whether results are measured or modelled, so the table is a map of what the literature reports, not a design specification.

Parameter	Reported value or range	Type	Source
Radius ratio $\lambda = R_{im}/R_{om}$ for power-dense designs	0.65–0.75	Design guidance from literature	[2]
Radius ratio maximising $\lambda(1-\lambda^2)$	0.577	Calculated	Section 2.2
Radial-flux aspect ratio above which AFPM torque exceeds RFPM (same R_{om}, equal loading)	about 12	Model	[2]
Torque per rotor volume, AFPM vs RFPM	Higher if $t_m + t_y < \lambda R_{om}/2$	Derived from sizing equations	[2]
Eddy-current loss reduction by 14 magnet segments per pole (4 kW, 2,500 rpm YASA)	factor of 8	Reported	[2]
Torque-density increase, quasi-Halbach vs axially magnetised array (torus)	12 %	Reported	[2]
Torque-density increase, sinusoidal interior-magnet rotor (coreless)	35 %	Reported	[2]
Cogging-torque reduction, alternating pole arcs / one-pole-pitch skew	73 % / 48 %	Reported	[2]
Torque density of a magnetically geared axial-flux generator, prototype / simulated designs	7.8 kN·m/m³ / up to 60 kN·m/m³	Prototype / simulation	[2]
Single-stator dual-rotor machine meeting hover requirements of a research eVTOL, lightest design	12 pole pairs, 27 slots, 9.28 kg	Optimisation result	[3]
Analytical model power under-prediction vs FEA	about 9 %	Model comparison	[3]
Three-dimensional FEA evaluations to reach a Pareto front (surrogate-assisted vs conventional)	163 vs 886	Computational	[2]
Ideal hover figure of merit	$FM = C_T^{3/2}/(\sqrt{2}C_P)$; 1 for the ideal rotor	Definition	[1]

5. Failure modes, testing and qualification

5.1 Regulatory frame

The EASA special condition for small-category VTOL-capable aircraft (SC-VTOL-02, Issue 2, June 2024) requires, for the equipment and systems it covers, that each catastrophic failure condition is extremely improbable and does not result from a single failure, each hazardous failure condition is extremely remote and each major failure condition is remote (VTOL.2510(a)) [6]. For Category Enhanced it also requires provisions for in-service monitoring of equipment and systems whose failure may have hazardous or catastrophic consequences (VTOL.2510(c)) [6]. The design-principles paragraph requires that the applicant prevent single failures from resulting in a catastrophic effect on the aircraft (VTOL.2250(c)) [6]. A NASA contractor report on reliability assessment of urban-air-mobility concepts, written against the earlier issue of the special condition, states the catastrophic-failure criterion as no more than 10^{-9} per flight hour with no single failure resulting in a catastrophic event, calls it the most challenging criterion, and lists motor and rotor overspeed protection among the areas needing further work [7]. These texts set requirements at aircraft level; how a machine's failure modes contribute to them is a design-specific analysis.

5.2 Inter-turn short circuit

For permanent-magnet machines the magnets cannot be de-excited, so a short-circuited turn continues to be driven by the rotating magnet field. Qin et al. note that the high current in the faulted turns can cause local overheating, damage the winding insulation and lead to breakdown of the entire phase, and can cause irreversible demagnetisation of the magnets [4]. They developed an analytical fault model driven by the PWM inverter voltages and validated it with tests; they report that, depending on the DC bus voltage and switching frequency, the PWM harmonic component can exceed 50 % of the fundamental component of the fault current, so sinusoidal-voltage models that neglect it under-estimate the fault current [4]. Quantities that affect the PWM ripple in the fault current include the number of short-circuited turns and the coil connection (series or parallel) [4]. For a drive that must survive such a fault, the thermal analysis of Section 2.4 therefore has to include the faulted condition, not only the healthy one. These results were obtained on machines that are not axial-flux, and no source reviewed here reports an AFPM-specific fault study at rotor-drive power.

5.3 Demagnetisation

Taras et al. compared single-layer and double-layer switched-flux machines for short-circuit current and irreversible demagnetisation using coupled Simulink and finite-element models, with measurements for the short-circuit current and demagnetisation [5]. They report that the topology with the lowest short-circuit current also had the best demagnetisation withstand capability, that the available magnet volume and the magnetic-circuit configuration, in addition to the short-circuit current, play an important role in demagnetisation, and that removing half of the magnets improved withstand capability and in-

creased torque per magnet volume [5]. The machine type differs from the axial-flux machines of this paper, so the quantitative results do not transfer; the methodological conclusion, that demagnetisation margin must be evaluated together with the fault-current prediction and the magnetic-circuit layout, does.

5.4 Testing and qualification evidence

The sources reviewed show three kinds of evidence that a qualification file would combine: validated analytical or network models of electromagnetic and thermal behaviour [3]; prototype measurements of loss, torque and temperature [2][4]; and fault tests or fault-model validation [4][5]. The review of axial-flux technology observes that most reported emerging topologies are at proof-of-concept level and that practical demonstrations at significant power scales have not been reported [2]. No source reviewed here defines a test standard for a lift-rotor motor; applicable environmental and software standards depend on the aircraft certification basis and were not reviewed.

6. Open problems and limitations

1. **Thermal validation at the continuous point.** The reviewed sources report lumped-parameter and finite-element thermal modelling, but the open sources read for this paper do not give measured continuous-torque data for machines at the 20–40 kW, 1,500 rpm operating point of Section 2.1. The achievable electric loading $\hat{A}$ at that point is therefore an assumption in the examples above.
2. **Single-failure tolerance of the machine itself.** Fault models exist for radial-flux and switched-flux machines [4][5]; an equivalent body of validated work for axial-flux machines with segmented or modular armatures at aircraft power is not present in the sources reviewed.
3. **Scaling of the torque relation.** The closed-form relation in [2] is stated for specific configurations (YASA, coreless, single-sided); its use for dual-rotor machines, as in [3], requires attention to the number of airgaps, and the definition of $\hat{A}$ must follow [2].
4. **Model-versus-measurement gap.** The 9 % analytical under-prediction reported in [3] is a model-to-model comparison; the paper’s abstract does not report a comparison with measurement.
5. **Load assumptions.** The worked examples assume a figure of merit, a tip speed and symmetrical load sharing; real duty cycles include manoeuvre loads, inertial transients when rotor speed changes, and the thrust needed for attitude control.
6. **Scope of the evidence.** Several percentage improvements in Table 1 come from individual simulations or prototypes at different scales and should not be combined.

7. Conclusions

- The hover load of a lift rotor is a low-speed, high-torque load: for the generic eight-rotor example, 190 N·m continuous and at least 232 N·m after the loss of one rotor, at about 1,500 rpm and 30–37 kW.
- The torque-sizing relation reported for axial-flux machines scales with the cube of the outer active radius and with $\lambda(1 - \lambda^2)$; the radius ratio of 0.65–0.75 used in power-dense designs gives 85–98 % of the maximum value of this factor (calculated: 0.328–0.375 against 0.385) while using less active area.
- An axial-flux machine is predicted to out-torque a radial-flux machine of the same outer radius only when the radial-flux machine is long relative to its diameter (diameter-to-length ratio above about 10–12), and the advantage in practice depends on the loading that cooling allows.
- Continuous rating is set by temperature; coupled magnetic-equivalent-circuit and thermal-network models validated against finite-element results are the reported route to a mass-optimal design at the hover point, with an analytical under-prediction of about 9 % in one study.
- Regulatory text requires that catastrophic failure conditions not result from a single failure; for permanent-magnet machines, inter-turn short-circuit and demagnetisation behaviour must be modelled with the PWM drive, and the open literature does not yet provide validated data of this kind for axial-flux machines at rotor-drive scale.

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