MASTER

Direct drive motors in tape feeder applications
a feasibility study

Philippi, C.T.

Award date:
2007

Link to publication
Capaciteitsgroep Elektrische Energietechniek
Electromechanics & Power Electronics

Master of Science Thesis

Direct Drive Motors in Tape Feeder
Applications: a Feasibility Study

C.T. Philippi
EPE.2007.A11

The department Electrical Engineering
of the Technische Universiteit Eindhoven
does not accept any responsibility
for the contents of this report

Coaches:

dr. E. Lomonova, TU/e
ir. R.A.J. van der Burg, Assembléon
ing. P. van Dijk, Assembléon

June 2007

/ faculteit elektrotechniek
Direct Drive Motors in Tape Feeder Applications: a Feasibility Study

Christiaan Philippi
Company: Assembleon & TU/e
Group: EPE
Supervisor TU/e: dr. E. Lomonova

June 29, 2007
Abstract

Assembléon is a Dutch manufacturer of pick-and-place machines, headquartered in Veldhoven. These machines are used to place all components on their positions on Printed Circuit Boards. In these machines the used components are delivered on tape. The unrolling and indexing of this tape is the task of the tape feeder.

These tape feeders currently make use of a brushed DC motor and a gearbox to drive the sprocket wheel which transports the tape. It is this gearbox that has several drawbacks: it is expensive, subjected to wear, introduces gearing backlash and additional power losses. Therefore, a new direct-drive concept using the so-called torque motor is proposed and analyzed.

In this work, the design procedure of the direct drive torque motor for feeder applications is developed. A MEC model is created in Matlab, and validated with numerical magnetostatic FEM results.

Using this design procedure, together with the MEC model, several design aspects are investigated. It is shown that the friction forces which are present in the system are a large limitation on the required motor torque, and thus dictate the required motor diameter. From these investigations, together with the cost analysis carried out by Ad Kieboom, a motor design is proposed.

Furthermore, trends in future feeders are discussed, together with their impact on achievable motor torque. It is shown that for future feeders, having even smaller dimensions, it is needed to decrease the required pull force in order to come to a feasible solution.

Keywords

Actuator design, direct-drive, Finite Element Analysis, Magnetic Equivalent Circuit and torque motor.
## Contents

1 Introduction .............................................. 1
   1.1 Description of the application ....................... 1
   1.2 Advantages of direct-drive .......................... 1
   1.3 Disadvantages of direct-drive ....................... 3

2 Specifications and requirements ................. 4
   2.1 Dimensions ........................................ 4
   2.2 Outermost pick position ............................ 4
   2.3 Electrical power .................................... 4
   2.4 Performance ....................................... 5
   2.5 Twin Tape Feeder [TTF] ............................. 5
   2.6 Intelligent Tape Feeder (ITF, single lane feeder) .... 5
   2.7 Load Dynamics and Motion Profiles ................. 6
      2.7.1 Inertia of 13 inch reel with tape ............... 6
      2.7.2 Triangular speed profile ....................... 7
      2.7.3 Trapezoidal speed profile ....................... 7
      2.7.4 Required force ................................ 8

3 Comparing different motor types ............... 10
   3.1 Permanent Magnet Brushless DC motor ............. 11
      3.1.1 Radial Flux PM BLDC motor .................... 11
      3.1.2 Axial Flux PM BLDC motor ..................... 12
   3.2 Stepper motor ..................................... 14
      3.2.1 Switched Reluctance stepper motor ............. 14
      3.2.2 Permanent Magnet Stepper motor ............... 14
      3.2.3 Hybrid steppers ................................ 16
   3.3 Ultrasonic Motor (USM) ............................ 16
   3.4 Conclusion ......................................... 18

4 Permanent Magnet Brushless DC Torque Motor ... 20
   4.1 Introduction ....................................... 20
   4.2 What is a torque motor? ........................... 20
   4.3 Initial design ..................................... 21
      4.3.1 Initial sizing ................................ 21
      4.3.2 Number of poles and slots .................... 23
      4.3.3 Winding layout ................................ 25
### 4.3 Material Selection

- **4.3.4 Magnet materials and sizing**: Page 25
- **4.3.5 Iron parts sizing**: Page 26
- **4.3.6 Slot depth and number of turns**: Page 27

### 4.4 FEM Model

- **4.4 FEM model**: Page 29

### 5 Optimizing the Design

- **5.1 MEC Model**: Page 38
  - 5.1.1 Reluctance calculations: Page 40
  - 5.1.2 MMF forces: Page 43
  - 5.1.3 MEC Solution Procedure: Page 43
  - 5.1.4 Node Potential Equations and Permeance Matrices: Page 43
  - 5.1.5 Including saturation effects: Page 47
  - 5.1.6 Torque production: Page 48
  - 5.1.7 Electrical equations: Page 49
  - 5.1.8 Model validation: Page 50
  - 5.1.9 Optimization routine: Page 50
  - 5.1.10 Conclusion: Page 53

### 6 Sensor Possibilities

- **6.1 Requirements**: Page 54
- **6.2 Possibilities**
  - 6.2.1 Hall-effect sensors: Page 54
  - 6.2.2 MR sensors: Page 55
  - 6.2.3 Optical encoders: Page 56
  - 6.2.4 Philips Twin Laser Technology: Page 56
- **6.3 Conclusion**: Page 57

### 7 Production Costs

- **7.1 Initial estimation**
  - 7.1.1 Stator parts: Page 58
  - 7.1.2 Rotor part: Page 59
  - 7.1.3 Additional parts: Page 59
  - 7.1.4 Total cost: Page 60
- **7.2 Extensive analysis**: Page 60

### 8 Performance Calculations

- **8.1 Diameter variation**: Page 63
- **8.2 Conical vs. rectangular coils**: Page 64
- **8.3 Magnet material**: Page 66
- **8.4 Core material**: Page 67
- **8.5 Power supply**: Page 68
- **8.6 Sinusoidal vs. square excitation**: Page 68
- **8.7 Number of poles and slots**: Page 68
- **8.8 Combining results**: Page 71
- **8.9 Proposed design**: Page 71
- **8.10 Reserving space for needle winder**: Page 73
- **8.11 Alternative design**: Page 73
## 9 Future feeders

9.1 Trends in feeder design ........................................ 76
9.2 Feeder dimensions .................................................. 76
9.3 Indexing time ......................................................... 77

## 10 Conclusions ...................................................... 78

Bibliography .......................................................... 80
Chapter 1

Introduction

In the development of the next generation of tape feeders, Assembleon is looking for a concept which is simple, robust and low cost. With this goal in mind, the task of this traineeship is to investigate possibilities to replace the currently used DC motor with transmission with a direct-drive solution.

1.1 Description of the application

Tape feeders are used in pick-and-place machines. These are machines used in the production process for Printed Circuit Boards. The task of a pick-and-place machine is to place all components on their position on the PCB. Most of these components are supplied on tape (Fig. 1.1), which is wound on a reel. It is the task of the tape feeder to unroll this tape, and deliver the components to the position were the robot arm with the placement head can collect them. This process is called indexing. Components are delivered one by one, directly after the robot has collected the previous component.

1.2 Advantages of direct-drive

As the requirements for machine tool productivity, accuracy and dynamic performance have increased, direct drive technology has emerged as an ideal way to meet these demands. Direct drive torque motors, in particular, have been demonstrated to provide significant machine tool performance improvements. In addition to providing high dynamic performance, torque motors can reduce machine cost of ownership, simplify the machine design, and reduce wear and maintenance. The most relevant benefits for the application under study:

- Direct-drive removes expensive gears
- Direct-drive is less subjected to wear
- Direct-drive does not have inaccuracies from gearing backlash
- Direct-drive does not have a power loss in gears
CHAPTER 1. INTRODUCTION

Figure 1.1: Components on a 8mm tape (placed at 4mm pitch)

Figure 1.2: Illustration of the current solution
1.3. DISADVANTAGES OF DIRECT-DRIVE

Direct-drive also introduces some problems, which should be taken into account:

- Without gearing motor needs to develop a very high torque at very low speed.
- Without a high gear ratio an encoder with a very high resolution is needed.
- Without high gear ratio operator can rotate the sprocket wheel by pulling tape.

Thus, the main goal of the project was to investigate and provide a feasibility studies of direct-drive solutions. In chapter 2 the specifications and requirements for such a solution are treated. Afterwards, in chapter 3, the torque motor and its initial design procedure are discussed in more detail. In chapter 4 a Magnetic Equivalent Circuit for such a motor is developed. In chapter 5 the sensing problem is discussed, and several possibilities are given. Chapter 6 gives an overview of the costs of the proposed solution, since this is a very important factor. After that several design possibilities and variables are compared in chapter 7. Then in chapter 8 the trends for future feeders are briefly discussed, together with their impact on possible solutions. Finally, in chapter 9 conclusions are drawn concerning the feasibility of direct-drive solutions, considering all aspects treated in the chapters before.
Chapter 2

Specifications and requirements

In this chapter all specifications and requirements which apply to the motor to be designed are specified, based on the envelope of the current feeders since these are well documented and provide a valuable guideline. When these guidelines are exceeded, the consequences have to be evaluated. Both the specifications for the Intelligent Tape Feeder (ITF) as those for the Twin Tape Feeder (TTF) are discussed. Later on, possible trends for future feeders are discussed, together with their impact on the results.

2.1 Dimensions

All feeders have the same height of 116 mm. This limits the diameter of the motor to approximately 95 mm, since the tape path has to fit in. The width of the usable area is probably not a limiting factor, and can be extended up to 150 mm. The depth of the feeders is different for various tapes. For example 15.6 mm for 8 mm tapes. When the thickness of the bottom and cover plate (1.1 mm) is taken into account only 13.4 mm is left, which is the limit of the axial length of the motor. Note that a TTF feeder needs two motors in the space described here. The specifications of the depth of all feeders is given in Table 2.3. An illustration of the available space in the tape feeders is given in Fig. 2.1.

2.2 Outermost pick position

Since the robot arm has to be able to collect the component from the pick position, this is limited to the working area of the robot arm. This means that the outermost pick position needs to be within 50 mm from the edge of the feeder. Putting it further away would require a redesign of the entire machines. Furthermore, it would increase the travel distance of the robot, which results in less output of the machine (components per hour).

2.3 Electrical power

The current feeders all work from the power supply of 12 VDC voltage. Maximum current which is available to the indexing motor is 400 mA. The feeder-interface is already prepared for a 24 VDC supply. Thus such a power supply is also available, if necessary. But total used power (4.8 Watt) should remain the same, limiting the current to 200 mA. Current feeders
2.4 Performance

The motor has to develop a torque such that the force pulling to unroll and move the tape is at least 15N. This level is based on the current used feeders. If, for example, the tape path is optimized in future feeders, required force could be less. The feeder has to be capable of taking 1, 2 and 4mm index steps, with sufficient accuracy. A static accuracy of 0.04mm per index position is required, with reproducibility per index position of 0.03mm. In current feeders a single step takes less than 200 ms. This is a good indication of what the next motor should be capable of. Note, that the current TTF sprocket wheel, for example, has 24 sprockets, thus with a 200 ms index time a single revolution already takes almost 5 seconds. Another requirement is the fact that the rotor position needs to be known at all time, even when the feeder is just switched on.

2.5 Twin Tape Feeder [TTF]

All specifications for the twin tape feeder are listed in Table 2.1. Note that there is only one width variant of the TTF, which carries two 8mm tapes in the feeder width of 15.6mm.

2.6 Intelligent Tape Feeder (ITF, single lane feeder)

The specifications for the ITF feeder are listed in Table 2.2. Note that the ITF feeder comes in different sizes, for the use of various tape widths (see Table 2.3).

---

12 indexing motors needed in this space, both with pick position in reach of the robot arm.
Table 2.1: TTF specifications

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum dimensions (length * width * height)</td>
<td>150 mm * 95 mm * 13 mm</td>
</tr>
<tr>
<td>Force [N]</td>
<td>15N</td>
</tr>
<tr>
<td>Supply voltage</td>
<td>12V -5+10% ripple &lt; 15% or 24V</td>
</tr>
<tr>
<td>Max. current</td>
<td>400mA (12V) or 200mA (24V)</td>
</tr>
<tr>
<td>Index distance [mm]</td>
<td>1 mm / 2 mm / 4mm</td>
</tr>
<tr>
<td>Static accuracy per index position [µm]</td>
<td>40 µm</td>
</tr>
<tr>
<td>Reproducibility per index position [µm]</td>
<td>30 µm</td>
</tr>
<tr>
<td>Max. indexing time [ms]</td>
<td>200 ms</td>
</tr>
<tr>
<td>Lifetime</td>
<td>10 years or 12.5 million indexes</td>
</tr>
</tbody>
</table>

Table 2.2: ITF specifications

<table>
<thead>
<tr>
<th>Specification</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum dimensions (length * width * height)</td>
<td>150 mm * 95 mm * max depth</td>
</tr>
<tr>
<td>Force [N]</td>
<td>15N</td>
</tr>
<tr>
<td>Supply voltage</td>
<td>12V -5+10% ripple &lt; 15% or 24V</td>
</tr>
<tr>
<td>Max. current</td>
<td>400mA (12V) or 200mA (24V)</td>
</tr>
<tr>
<td>Index distance [mm]</td>
<td>1 mm / 2 mm / 4mm</td>
</tr>
<tr>
<td>Static accuracy per index position [µm]</td>
<td>40 µm</td>
</tr>
<tr>
<td>Reproducibility per index position [µm]</td>
<td>30 µm</td>
</tr>
<tr>
<td>Max. indexing time [ms]</td>
<td>200 ms</td>
</tr>
<tr>
<td>Lifetime</td>
<td>10 years or 12.5 million indexes</td>
</tr>
</tbody>
</table>

Table 2.3: Different tape widths

<table>
<thead>
<tr>
<th>Tape width [mm]</th>
<th>Feeder width [mm]</th>
<th>Maximum depth actuator [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>15.6</td>
<td>12</td>
</tr>
<tr>
<td>12</td>
<td>19.6</td>
<td>16</td>
</tr>
<tr>
<td>16</td>
<td>23.6</td>
<td>20</td>
</tr>
<tr>
<td>24</td>
<td>31.6</td>
<td>28</td>
</tr>
<tr>
<td>32</td>
<td>39.6</td>
<td>36</td>
</tr>
<tr>
<td>44</td>
<td>51.6</td>
<td>48</td>
</tr>
<tr>
<td>56</td>
<td>63.6</td>
<td>60</td>
</tr>
</tbody>
</table>

2.7 Load Dynamics and Motion Profiles

2.7.1 Inertia of 13 inch reel with tape

The inertial mass of a reel can be calculated using the expression for inertia of a cylinder:

\[ J_{\text{reel}} = \frac{1}{2}m_{\text{reel}}r_{\text{reel}}^2 [kgm^2]. \]  

(2.1)

A typical 13 inch reel has a mass of circa 1 kg. This gives an inertia equal to: \[ 0.5 \cdot 1 \cdot (2.54 \cdot 10^{-2} \cdot 0.5 \cdot 13)^2 = 1.36 \cdot 10^{-2}kgm^2. \] This is for a reel which is still full with tape, thus worst case. The value of inertia will drop with the unrolling of the tape. Since the inertia of the reel is known, it is possible to calculate the force which is required for a given speed profile, while this is proportional to the required acceleration. Thus, first several possible speed profiles are stated, together with their maximum speed, acceleration (torque) and jerk.
2.7. LOAD DYNAMICS AND MOTION PROFILES

2.7.2 Triangular speed profile

Each speed profile has to satisfy two constraints. Total displacement of the tape has to be exactly 1 index position \((x = 1, 2 \text{ or } 4 \text{mm})\) over the time interval \(\tau = 100\text{ms}\). And the speed at the end of the time interval has to be equal to zero:

\[
\int_0^\tau v \, dt = x \, [m],
\]
\[
v(\tau) = 0 \, [m/s].
\]

The first speed profile is the triangular one. This is plotted in Fig. 2.2, together with the corresponding displacement and acceleration.

Calculation of the maximum speed and acceleration levels will be as:

\[
\int_0^\tau v \, dt = \frac{1}{2} \tau v_{\text{max}} = x \, [m],
\]
\[
v_{\text{max}} = \frac{2x}{\tau} \, [m/s],
\]
\[
a_{\text{max}} = \frac{v_{\text{max}}}{0.5\tau} = \frac{4x}{\tau^2} \, [m/s^2].
\]

2.7.3 Trapezoidal speed profile

Another speed profile is the trapezoidal one. This profile is described by one third of the time acceleration, one third of the time constant speed and one third of the time deceleration. This results in a lower maximum speed, but higher acceleration levels. Maximum speed and
acceleration can be calculated as follows:

\[ \int_0^\tau v dt = \frac{2}{3} \tau v_{\text{max}} = x \, [m], \]  

\[ v_{\text{max}} = \frac{1.5 \tau}{\tau} \, [m/s], \]  

\[ a_{\text{max}} = \frac{v_{\text{max}}}{0.33 \tau} = \frac{4.5 \tau}{\tau^2} \, [m/s^2]. \]  

Note, that both of the profiles require very fast changes in acceleration. This means that the bandwidth of the torque / current controller has to be very high. In motors with a high stator inductance this is impossible to realise. A profile with a limited jerk (derivative of acceleration) requires less bandwidth, but needs a higher maximum acceleration (and thus torque). An example of such a profile is given in Fig. 2.3.

2.7.4 Required force

With the maximum acceleration of each profile, and the inertia of the reel with tape, the force needed to realise this acceleration can be calculated. First the acceleration is converted from meters per second squared, to radians per second squared:

\[ \omega = \frac{a_{\text{max}}}{2\pi r_{\text{reel}}} \cdot 2\pi = \frac{a_{\text{max}}}{r_{\text{reel}}} \, [rad/s]. \]  

The torque necessary to realise this acceleration is given by:

\[ \omega = \frac{1}{J_{\text{reel}}} T_{\text{reel}} \, [rad/s]. \]  

Then the force on the tape has to be equal to:

\[ F_{\text{acc}} = \frac{T_{\text{reel}}}{r_{\text{reel}}} = \frac{J_{\text{reel}} a_{\text{max}}}{r_{\text{reel}}^2} = \frac{1}{2} m_{\text{reel}} a_{\text{max}} \, [N]. \]  

Doing these calculations indicates that a 13inch reel with a mass of 1 kg requires a force on the tape of only 1.2 Newton to realise an acceleration of 2.5 \, m/s^2. In the case of no friction, this is equal to the force that the motor has to develop. But there is a significant friction force present. The existing friction force heavily depends on the thickness of the tape, the type of tape which is used (paper, plastic) and the possible splicing method used. It is given from
earlier measurements that in the current feeders this can reach up to 10 Newtons. Because of this high dependency on the used tape, total required force $F_{\text{req}}$ is set to a worst-case value of 15N over the entire speed range from 0 to 0.08 m/s, which should be sufficient for any tape used. Since designing of rotating motors always uses the torque requirements as a starting point, the pulling force needs to be translated to minimum torque constraint:

$$T_{\text{req}} = F_{\text{req}} R_o = 15 R_o = 7.5 D_o,$$

$$\omega_{\text{max}} = \frac{v_{\text{max}}}{R_o} = \frac{0.08}{R_o} = \frac{0.16}{D_o},$$

with $R_o$ -the motor radius and $D_o$ -the motor diameter. Thus the required torque is constant over the whole speed range, but is dependent on the radius of the motor. This is illustrated in Fig. 2.4.
Chapter 3

Comparing different motor types

In this chapter, several different types of motors will be discussed and compared. At the end of this chapter the decision which type of motor is best for the direct-drive feeder application will be made, using the results found during this chapter. Several types of motors will be investigated:

- BLDC PM Synchronous Machine
- Stepper motor
- Ultrasonic piezo motor

Before a good comparison can be made, the points on which the motors are being compared will be given.

- Required volume (or in which feeders it can be used)
- Performance
- Impact on power electronics
- Control effort to obtain accuracy
- Sensing
- Lifetime, wear, reliability
- Production efforts and cost

*Performance:* In this case performance generally means that the motor needs to be able to generate as much torque from the smallest volume, thus have a high torque per volume ratio (TPV).

*Impact on power electronics:* Different motors require different drives. Motors which can be driven by a simple drive is preferred over a motor which requires complex drive electronics.

*Control effort:* In fact the control effort is the cost to control the motor with sufficient accuracy. This incorporates the cost of the computational power needed to control the motor. A motor which doesn’t need additional hardware for computations is preferred.

*Sensing:* The cost of the sensors needed to obtain a certain resolution. Most methods of sensing are applicable to all types of motors, but some motors might have specific advantages
3.1 Permanent Magnet Brushless DC motor

Of all the candidates, the brushless DC permanent magnet motor (PMBLDC) is the one with the highest possible torque densities. This is due the presence of permanent magnet excitation and the absence of brushes with friction and losses. But this comes at a cost, permanent magnets make the motor more expensive and introduce cogging, and the absence of brushes requires more complicated drive systems to take care of the commutation. Typical PMBLDC motors are 3 phase and have drives with 6 switches for the commutation, but techniques using only 3 are available. PMBLDC motors can be divided into 2 types: The ones with in which the air gap flux flows radially through the air gap (radial flux RFPM), and the ones in which the flux through the gap flows parallel to the shaft (axial flux AFPM). Each one of these two will be discussed separately, since motor topologies are quite different.

3.1.1 Radial Flux PM BLDC motor

As said before, radial flux machines have the flux flowing radially through the air gap. They are very similar to the brushed DC motors. The difference comes from the fact that in the
BLDC motors the permanent magnets are rotating, instead of the windings. These windings generate a rotating magnetic field in the air gap, at which the magnets on the rotor are trying to align. Radial flux PM BLDC motors can be separated into two different categories, the ones with an inner rotor and those with an outer rotor. In Fig. 3.2 a schematic drawing of both can be found. On the left a motor with inner rotor, and on the right one with an outer rotor. Since the principles are the same, they will not be discussed separately. Note that the schematic drawing of the outer rotor BLDC is exactly the same as for the brushed DC motor, but has the words rotor and stator interchanged. Outer rotor motors have the benefit of the larger airgap diameter, giving the possibility for higher torque levels. The drawback of having the rotating part on the outside is the larger inertia. This makes them less attractive for start-stop applications. However, when speeds are as low as in this application, the torque required to accelerate this inertia remains very small compared to the load torque. Furthermore when the rotating part is on the outside, the sprocket teeth can be directly on the back iron, instead of having an extra wheel on the axis. Therefore for this application an outer rotor structure is desirable.

<table>
<thead>
<tr>
<th>Performance</th>
<th>Radial flux PM BLDC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Impact on PE</td>
<td>3-phase PWM driven, 6 switches</td>
</tr>
<tr>
<td>Control effort</td>
<td>Heavily dependent on available sensors</td>
</tr>
<tr>
<td>Sensing</td>
<td>Lot of possibilities (Hall, MR, optical, .)</td>
</tr>
<tr>
<td>Lifetime, wear</td>
<td>No brushes, only bearing wear</td>
</tr>
<tr>
<td></td>
<td>Permanent magnets temperature dependencies, risk of demagnetisation</td>
</tr>
<tr>
<td>Production costs</td>
<td>Standard materials and parts</td>
</tr>
</tbody>
</table>

### 3.1.2 Axial Flux PM BLDC motor

Axial flux PM BLDC motors are in principle the same as the radial flux counterparts. But instead of flux flowing in radial direction, it flows parallel to the axis. This gives the possibility to have a much larger air gap area at high radius / length ratios, where energy is transferred. Main issue is the limited height of around 13mm for a single lane feeder. There are some
3.1. PERMANENT MAGNET BRUSHLESS DC MOTOR

possibilities to reduce the height of an axial flux motor, for example using an ironless stator with the windings etched on a PCB. But efficiency of these drives is significantly less. Another disadvantage of the axial flux motor, is the fact that there is a high attracting force between the stator and the rotor disc, which increases with smaller air gaps. This force in axial direction gives extra stress on bearings. A way to cancel this effect is using a double sided axial flux motor with two rotor discs, one on each side of the stator. But the limited height makes this impossible.

<table>
<thead>
<tr>
<th>Table 3.2: Axial flux PM BLDC</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Axial flux PM BLDC</strong></td>
</tr>
<tr>
<td>Performance</td>
</tr>
<tr>
<td>Impact on PE</td>
</tr>
<tr>
<td>Control effort</td>
</tr>
<tr>
<td>Sensing</td>
</tr>
<tr>
<td>Lifetime, wear</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Production costs</td>
</tr>
<tr>
<td></td>
</tr>
</tbody>
</table>
3.2 Stepper motor

Stepper motors are motors in which a single revolution is divided in a fixed number of steps. The rotor moves from fixed position to position. This is exactly the behaviour which is required in the tape feeder application. Tape should be unrolled in fixed steps. In a stepper motor, step size is fixed by the architecture of the motor. There are several types of stepper motors:

- Variable Reluctance
- Permanent magnet
- Hybrid stepper

Each one will be discussed here.

3.2.1 Switched Reluctance stepper motor

Switched Reluctance stepper motors work on a very simple principle which is illustrated in Fig. 3.5. When a coil is excited, it becomes a magnetic pole, and the iron teeth at the rotor are attracted. It comes to a halt in the position at which reluctance ($R_m$ magnetic resistance) is minimal and flux ($\phi$) is maximal. In the case of no load the teeth will perfectly align, but when there is a load at the shaft the rotor will stop at the point where the attracting force equals the load. This is a static error. Therefore, the maximum force of the motor needs to be several times the maximum loading force, in order to minimize the static error. This is illustrated by Fig. 3.6 which shows the typical shape of the torque vs. displacement graph of a SR motor. Also the fact that there is no permanent magnet excitation in this motor, limits its torque capability.

<table>
<thead>
<tr>
<th>Table 3.3: Switched reluctance stepper motor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Performance</td>
</tr>
<tr>
<td>Impact on PE</td>
</tr>
<tr>
<td>Control effort</td>
</tr>
<tr>
<td>Sensing</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Lifetime, wear</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Production costs</td>
</tr>
<tr>
<td></td>
</tr>
</tbody>
</table>

3.2.2 Permanent Magnet Stepper motor

The PM stepper motor is in fact the same as a BLDC PM motor, driven as a stepper motor. In order to achieve a high number of steps / rev a very high pole count is required. That is the reason why PM stepper motors usually have a relatively low steps / rev count. A typical step size is $7.5^\circ / 15^\circ$. This makes the PM stepper motor unsuitable for our application and will not be further discussed. Another type of stepper motor deploying the benefits of a permanent magnet and the high step resolution of the SR motor is the hybrid stepper motor.
Figure 3.5: principle of switched reluctance motor [6]

Figure 3.6: Torque vs. displacement
3.2.3 Hybrid steppers

The hybrid stepper motor is a rather complex motor design. It is capable of realizing step angles of 0.9 / 1.8 degrees, which give up to 400 steps per revolution. The architecture of a hybrid stepper motor is shown in Fig. 3.8 and 3.9. The hybrid motor contains 2 toothed discs, which are axially displaced with a permanent magnet in between. This magnet is magnetized in the axial direction, turning all teeth of the first disc into north poles and all teeth on the second disc in to south poles. Now when a phase is excited it attracts either a north pole or a south pole, depending on the direction of the current. An illustration of the operation of a hybrid stepper motor is given in Fig. 3.10.

<table>
<thead>
<tr>
<th>Hybrid stepper motor</th>
<th>Performance</th>
<th>Impact on PE</th>
<th>Control effort</th>
<th>Sensing</th>
<th>Lifetime, wear</th>
<th>Production costs</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Capable of producing sufficient torque</td>
<td>Needs bipolar switching, H-bridges</td>
<td>Easy to control</td>
<td>Lot of possibilities (Hall, MR, optical, ...)</td>
<td>No brushes, PM risks</td>
<td>Most complex design</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Can be done with less accurate sensors</td>
<td></td>
<td>Requires very accurate machining, thus higher costs are expected</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

3.3 Ultrasonic Motor (USM)

The Ultrasonic motor is a motor which uses the piezo electric effect instead of electromagnetic effect to put force on to the rotor. Motor consists of a stator with piezoelectric material, and a rotor which is pressed onto it. Now by applying voltages to the piezo material, a traveling
3.3. ULTRASONIC MOTOR (USM)

Figure 3.8: Hybrid stepper motor cut-through (axial direction) [1]

Figure 3.9: Hybrid stepper motor cut-through (radial direction) [1]

Figure 3.10: Hybrid stepper operation [6]
wave in the stator is generated. Now due to the orbital motion of the contact area and the friction in the contact area, the rotor is pushed in opposite direction of the traveling wave (see Fig. 3.11).

Benefits of USM:

- Very high torque and low speed
- Static force when not excited equal to peak force

Drawbacks of USM:

- Operates at high voltages
- Lot of heat due to friction these motors generally have a short life time.
- Due to the friction
- Commercial available drives are expensive

Overall, the USM might be a very good candidate. But since these operate on a completely different phenomenal, out of the scope of my research.

3.4 Conclusion

At this point the best alternative needs to be chosen, in order to make full and accurate (FEM) calculations on that specific type. Therefore all scores of the alternatives are listed in table 3.4.

It can be seen that only motors which use permanent magnets are capable of realizing the very high torque density required. This leaves 3 candidates:

- Radial flux BLDC
- Axial flux BLDC
- Hybrid stepper motor
Because of the high complexity of the design of the hybrid stepper motor and its limited accuracy this seems to be the least interesting candidate. An axial flux BLDC motor seems very hard to realize within the height of 13mm, and is expected to be more expensive since it uses more permanent magnet material. Due to this fact, the radial flux motor is a better candidate. The radial flux can be found in many low cost applications, for example in CD and DVD drives. From first calculations it is expected that a radial flux machine is capable of producing sufficient torque, the next step is doing more accurate calculations including all kind of saturation and other non-ideal effects, resulting in an accurate estimation of the performance.

<table>
<thead>
<tr>
<th>Type</th>
<th>Specs</th>
<th>Power Elec.</th>
<th>Control</th>
<th>Sensing</th>
<th>Lifetime</th>
<th>Prod. &amp; Costs</th>
</tr>
</thead>
<tbody>
<tr>
<td>RF PM BLDC</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>0</td>
<td>+</td>
</tr>
<tr>
<td>AF PM BLDC</td>
<td>+</td>
<td>-</td>
<td>0</td>
<td>+</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Var. Rel. step</td>
<td>--</td>
<td>+</td>
<td>+</td>
<td>++</td>
<td>++</td>
<td>+</td>
</tr>
<tr>
<td>Hybrid stepper</td>
<td>+</td>
<td>-</td>
<td>+</td>
<td>++</td>
<td>0</td>
<td>-</td>
</tr>
</tbody>
</table>

Table 3.5: Comparison
Chapter 4

Permanent Magnet Brushless DC Torque Motor

4.1 Introduction

In this chapter the torque motor is introduced. After that an initial design for the given specifications and requirements is derived, and analysed using Finite Element Methods (FEM). In the next chapters this design will be further improved. An overview of the design process is given in Fig. 4.1.

4.2 What is a torque motor?

Torque motors are a special class of brushless permanent magnet servomotors. This type of motor is also commonly referred to as a permanent magnet synchronous motor or a brushless DC motor. This motor technology has many advantages over other types, for example:

- very small electrical time constraints, resulting in a high dynamic response,
- large mechanical airgap (0.5 - 1.5mm), resulting in easy mounting and alignment,
- high efficiency due to the use of permanent magnets.

From the point of view of general electrical theory of operation, torque motors are not different from their more conventional counterparts. Therefore, the electrical control requirements are fundamentally the same. However, in many ways the similarities end there. It is their specific differences that give torque motors their unique advantages for machine tool applications. The most unique feature of a torque motor concerns the physical dimensions. They have a relatively large diameter to length ratio, and they also have a rather short axial length. Additionally, torque motors can simultaneously have both a very large OD (outer diameter) and ID (inner diameter), resulting in a motor that is a thin ring. One important outcome of this characteristic is that the mass is quite low as a function of a diameter. Also, the large diameter allows very high torque to be developed. Torque motors are a type of "frameless" motor. This means that the motor does not include a housing, bearings, or feedback device. In this sense the motor is a "kit" motor, meant to be integral part of the machine structure. To assist integrating torque motors, they can be provided with a reusable assembly called a
4.3 Initial design

In this section the procedure to come to an initial design fulfilling the stated specifications and requirements is described [5]. Designing a motor always start with initial sizing equations, which give a rough estimate of the required volume.

4.3.1 Initial sizing

Initial sizing of a motor design is based on a simple approach called volumetric ($D^2l$)-sizing. The $D^2l$-estimate is a very common technique to easily identify the initial size of a motor, given its torque requirement. It is based on practical numbers for electrical and magnetic loading factors and the torque that the motor produces with these values of loading.

The specific electrical loading can be seen as an equivalent surface current density along the stator bore diameter and is equal to:

$$J_s = \frac{ZI_{ar}}{\pi D_{gap}},$$  (4.1)
with $J_s$-the specific electrical loading \([\text{A/m}]\), $Z$-the total number of conductors in the motor, $I_{ar}$-the armature current in Ampere and $D_{gap}$-the diameter of the air gap \([\text{m}]\). Typical values for $J_s$ are between 10 and 15 kA m\(^{-1}\). The amount of electrical loading typically comes from the electrical design of the motor, thus the stator part.

The specific magnetic loading is the average flux density \([\text{T}]\) over a pole span (or the average airgap flux density) and typically comes from the magnetic design (mainly the rotor part) (see Fig. 4.2):

$$B_{ml} = \frac{\phi_m}{\pi D_{gap}l/p},$$

(4.2)

where $B_{ml}$ is the specific magnetic loading, $\phi_m$ is the magnet flux per pole \([\text{Wb}]\), $l$ is the active length of the motor (in axial direction) \([\text{m}]\) and $p$ is the number of poles. Typical values are: $B_{ml} = 0.5 - 1$ [T]

Now, using the expression for Lorentz force, the torque produced by the ideal motor having these loadings can be calculated:

$$T = B_{ml} \cdot I_{tot} \cdot l \cdot r = B_{ml} \cdot J_s \pi D_{gap} \cdot l \cdot \frac{D_{gap}}{2}$$

(4.3)

$$T = \frac{\pi D_{gap}^2}{2} B_{ml} J_s l = 2 \left(\frac{\pi}{4} D_{gap}^2 l\right) B_{ml} J_s = 2V_{stator} B_{ml} J_s$$

(4.4)

This is a very important equation, since it clearly states the torque capability of a motor. Another important number, the Torque per Volume, can easily be determined to be equal to $2B_{ml} J_s$. This clearly shows that the amount of torque which can be produced from a fixed volume, is only dependent on the electrical and magnetic loading. Combining 4.4 with 2.13
4.3. INITIAL DESIGN

results in the minimum motor diameter:

\[ F_{\text{req}} \frac{D_o}{2} = 2 \left( \frac{\pi}{4} D_{\text{gap}}^2 l \right) B_{av} J_s,\]  
\[ D_o \approx 1.1 D_{\text{gap}},\]  
\[ \frac{1.1 F_{\text{req}}}{2} = 2 \left( \frac{\pi}{4} D_{\text{gap}} l \right) B_{av} J_s,\]  
\[ D_{\text{gap}} = \frac{F_{\text{req}} \times 1.1}{4 \left( \frac{\pi}{4} l \right) B_{av} J_s},\]  
\[ D_{\text{gap}} = 83.4 \text{ mm} \quad D_o = 92 \text{ mm}.\]

in which an estimated value of \( l = 7\text{mm} \) is chosen, which is an estimate of the maximum available active length in a feeder with a width of 16\text{mm}. A magnetic loading of 0.9T is selected, and an electric loading of 10kA/m. This is illustrated in Fig. 4.3.

Having the outer diameter of the motor, the corresponding maximum speed can be calculated:

\[ n = 60 \frac{\nu_{\text{max}}}{\pi D_o} = 16.6 \text{ rpm}.\]

4.3.2 Number of poles and slots

The first made decision, is the fact that the motor should have concentrated windings. Concentrated windings have the benefits of ease of winding and assembly, and have the minimum amount of end windings (length of turns exceeding stator part to cross to another slot, not contributing to the torque). The selection of the number of poles and slots was done on the basis of several papers. These papers state that one should select the number of poles and slots such that the highest winding factor is achieved, since these motors have the highest performance [2][10]. The winding factor is a measure for the ratio of flux which is actually linked by the windings, and is equal to 1 in the ideal case.
Table 4.1: Winding factors

<table>
<thead>
<tr>
<th>slots</th>
<th>2</th>
<th>4</th>
<th>6</th>
<th>8</th>
<th>10</th>
<th>12</th>
<th>14</th>
<th>16</th>
<th>18</th>
<th>20</th>
<th>22</th>
<th>24 poles</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td></td>
</tr>
<tr>
<td>12</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td></td>
</tr>
<tr>
<td>15</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td></td>
</tr>
<tr>
<td>18</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td></td>
</tr>
<tr>
<td>21</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td></td>
</tr>
<tr>
<td>24</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.866</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td>.945</td>
<td></td>
</tr>
</tbody>
</table>

Thus, the best performing combinations are those with the highest winding coefficient of 0.966:

- 10 poles - 12 slots
- 14 poles - 12 slots
- 16 poles - 12 slots
- 20 poles - 24 slots
- 28 poles - 24 slots
- 30 poles - 36 slots

After that, it is stated that machines with a number of slots per pole and per phase between 1/2 and 1/3 generally present higher performance characteristics [2], limiting the choice to:

- 10 poles - 12 slots
- 20 poles - 24 slots
- 30 poles - 36 slots

These possibilities are then compared, and it is shown in [10] that of this possibilities, the one with 20 poles and 24 slots has the highest value of torque per volume. Therefore, this one is selected. But the arrangement with 30 poles and 36 slots might also be an option, since the higher number of slots has the benefit of reducing end windings. But this also results in narrower magnets, teeths and slots, which might result in manufacturing difficulties. Naturally, the amount of slots and poles also depend on the size of the motor, but 20 magnets and 24 slots results in magnet and teeth dimensions (of around 10mm and 5mm width, respectively) which are very reasonable.
4.3. INITIAL DESIGN

4.3.3 Winding layout

The method to come to the optimal winding layout for motors with concentrated windings is fully described in [2]. The complete method is not repeated here, only the result is given. When this method is used, the resulting winding layout is: This layout is identical to the lay-out which the software package RMxpert proposes for this number of slots and poles, and is given in Fig. 4.4.

4.3.4 Magnet materials and sizing

The most important properties in the selection of a magnet material are those that define the magnitude and stability of the field it produces. These include the coercivity $H_c$, saturation magnetization $M_s$ and remenance $B_r$, as well the BH-curve in the second quadrant. This portion of the hysteresis loop is called the demagnetization curve. When a magnet is used as a field source, it becomes biased at a certain point $(B, H)$ on the demagnetization curve, with the corresponding energy product $BH$. This point is called the operating point. This point depends on the circuit in which the magnets are used, and can be derived using the loadline. To maximize the energy produced by the magnets (thus minimizing the required magnet volume), the operating point should be near $BH_{max}$, the point at which the energy product is maximal. The slope of the loadlines is the so called Permeance Coefficient, which is equal to:

$$PC = \frac{1}{f_{lkq}} \frac{h_m A_g}{g A_m}$$

in which $f_{lkq}$ is the leakage coefficient (ratio between total magnet flux and flux crossing the airgap), $h_m$ is the height of the magnets, $g$ is the equivalent airgap length (taking Carters coefficient for stator slotting in to account) and $A_g/A_m$ is the ratio between airgap area and magnet area, thus the amount of area covert by magnets. This is typical around 70%, thus $A_g/A_m$ is 1.43. The leakage factor is initially estimated to have a value of 0.85. This number is later verified using FEM calculations. According to [5] a good initial value for the PC is 10.

There are several different types of permanent magnet materials. These are Ferrites,
Alnico, Samarium-Cobalt and Neodymium-Iron-Boron. An overview of these materials and their properties is given in [3] chapter 1.14 - 1.17. A more extensive treatment is given in [7].

The most important aspect for the current design is the fact that it needs to develop a high torque from a very small volume. This requires magnets with a high energy product, and a high remanence. This limits the choice of the material to the NdFeB type. The selected magnet material is NdFeB-35, which is the cheapest grade of sintered neodymium. This means that the no load flux density of the permanent magnets \( B_{m0} \) will be 1.1T (see Fig. 4.5). Later on, the effect of using stronger magnets on the performance is evaluated. The airgap is selected to be 0.5mm, thus resulting magnet height is 3mm.

### 4.3.5 Iron parts sizing

From the loading point of the permanent magnets, the flux crossing the airgap can be calculated. The iron parts need to carry this flux, and should be sized sufficiently large, in order to avoid saturation:

\[
\phi_{m0} = B_{m0}A_m = 1.1 \frac{0.7\pi D_{gap}l}{p},
\]
(4.12)

\[
\phi_{tot} = \phi_{m0} \cdot p,
\]
(4.13)

\[
\phi_{gap} = f_{tkg} \cdot \phi_{tot},
\]
(4.14)

with \( \phi_{m0} \) -the no load flux of a single magnet, \( B_{m0} \) -the no load magnet flux density, \( \phi_{tot} \) -the total flux of all magnets and \( \phi_{gap} \) -the total flux crossing the gap. When a teeth is directly facing a magnet, all the magnet flux will flow through that teeth:

\[
\phi_p = f_{tkg} \phi_{m0}
\]
(4.15)

\[
B_t = \frac{\phi_p}{w_t l}
\]
(4.16)

Now limiting the flux density within the teeth up to 1.7T, the necessary width of the teeth can be expressed as:
4.3. INITIAL DESIGN

The same method is used to determine the minimum height of the rotor back-iron. The flux of one magnet pole is split in two paths, one half flows to the magnet to the left, and the other half flows to the right. This is illustrated in Fig. 4.6. Thus, the flux carried by the back iron is half the flux per pole:

\[ B_{bi} = \frac{\phi_{m0}}{2h_{bi}}, \]  

\[ h_{bi} = \frac{\phi_{m0}}{2 \cdot 1.7l}, \]  

with \( B_{bi} \) the back-iron flux density, and \( h_{bi} \) the height of the back-iron. Now, the height of the magnets and the back-iron are known, the value of outer diameter can be calculated, and it can be checked whether the assumption of Eq. 4.6 for the ratio outer diameter / airgap diameter was correct.

\[ D_o = D_{gap} + 2(Lm + h_{bi}) \]  

If the assumption was not correct, the value is adjusted and all calculation steps iteratively are done again. After some iterations all sizes are found and listed in Table 4.2.

| \( D_o \) | 98.7mm |
| \( D_{gap} \) | 86.6mm |
| \( h_m \) | 3.0mm |
| \( h_{bi} \) | 3.1mm |
| \( \psi_t \) | 4.4mm |

4.3.6 Slot depth and number of turns

Now there are only two important variables left to determine: the depth of the slots, and the number of conductors in the slots. Since the task is to get the maximum torque out of the volume, it is neccessary to have as much slot area as possible. This will give the oppertunity to get the lowest winding resistance / highest number of turns, and thus maximum torque capability. Since the width of the teeth are already fixed by the iron sizing rules, the maximum
depth of the slots are easily determined.

\[ h_t = \frac{D_{\text{gap}} - \frac{N_s (w_t + w_{\text{bottom}})}{\pi}}{2} \]  

(4.21)

\[ w_{\text{bottom}} = 3.5 \text{mm} \]  

(4.22)

In this equation \( w_{\text{bottom}} \) is the minimal bottom slot width, which is set to 3.5mm to ensure that is possible to place windings all the way down to the bottom of the slot, and to avoid very tight corners in the iron at the bottom. Having set this minimum, together with the gap diameter and the width of the teeth, we automatically get the maximum height of the slots. This results in a slot depth of 13.3mm.

Now only the number of conductors is left to determine. One way to do this is to simply look at the emf-voltage at the rated speed, which should be high enough to have an efficient motor. This results in many turns and very thin wires. But this overlooks the fact that supply voltage is limited, and the power supply might be unable to put sufficient current in the windings since winding resistance is very high. Therefore electrical loading is calculated for varying number of conductors, while taking voltage and current limits in to account. These expressions are for a 3-phase machine with square wave excitation:

\[ I(\omega_m) = \min \left( I_{\text{max}} \frac{V_{\text{max}} - E(\omega_m)}{R_{\text{ph-ph}}} \right) \]  

(4.23)

\[ R_{\text{ph-ph}} = \frac{2}{3} Z \left( \frac{l_t}{2\sigma A_{\text{wire}}} \right) = \frac{2}{3} Z^2 \left( \frac{l_t}{2\sigma k_f A_{\text{slots}}} \right) \]  

(4.24)

\[ k_e = \frac{2}{3} k_w Z \left( \frac{D_{\text{gap}}}{2} \right) B_{\text{gap}} l \]  

(4.25)

\[ J_s = \frac{Z I}{\pi D_{\text{gap}}} \]  

(4.26)

\[ \text{with } k_e, \text{- the backEMF constant, } I, \text{- the motor current, } R_{\text{ph-ph}}, \text{- the phase-to-phase resistance, } A_{\text{wire}}, \text{- the cross-sectional area of the wire, } l_t, \text{- the length of a single turn, } \sigma, \text{- the conductivity of copper, } \omega_m, \text{- the mechanical speed of the rotor in rad/s, } k_w, \text{- the winding factor and } A_{\text{slots}}, \text{- the total area of slots.} \]

Now the amount of conductors \( Z \) is determined such that the electrical loading \( J_s \) at rated speed, is maximized:

\[ Z = \arg\max_{Z \in \mathbb{N}} J_s (\omega_m = w_{\text{rated}}) \]  

(4.28)

This yields the number of conductors which maximize torque at the rated (tape) speed. A graphical representation of this approach is given in Fig. 4.7. Here, one can already see whether the initially estimated value of electrical loading was realistic, since here the maximum obtainable value given the dimensions and power supply is calculated. This results in a number of 9554 conductors in the machine. Usually it is more convenient to use the number of turns per coil (wound around a single teeth). This is equal to:

\[ N = \frac{Z}{2N_s} \]  

(4.29)
4.4 FEM MODEL

Electrical loading at rated speed

![Graph showing electrical loading at rated speed](image)

Torque at rated speed

![Graph showing torque at rated speed](image)

Figure 4.7: Determination of the number of conductors, by maximizing the electrical loading

The factor 2 is coming from the fact that there are two conductors needed to form a single turn. Furthermore there are $N_s$ slots, thus also $N_s$ coils.

Since that all initial dimensions and quantities are known (see Table 4.3, it is possible to calculate some first performance estimates. This is done with equations 4.23 to 4.28 combined with:

\[ T(\omega_m) = k_v I(\omega_m) \]  \hspace{1cm} (4.30)

This results in the torque-speed characteristic given in Fig. 4.8. As can be seen, the estimated torque for the motor is not reaching the minimum required torque, while it was sized for it. This comes from the fact that the magnetic loading which is realized ($B_{gap}$) is not reaching the initial estimated values ($B_{ml}$). This can be seen in Table 4.4. In order to correct this effect, the initial guessed loadings can be adjusted, to iteratively come to a design. But first the current derived sizes for the motor are used and analysed in FEM, to check whether the sizing equations, the calculated airgap flux density and the estimated value of the leakage factor are correct.

4.4 FEM model

In order to see whether the previous derived performance estimates are correct, a FEM model was created. Furthermore, this model can be used to verify and/or adjust initial estimated values for the leakage coefficient, the magnetic loading etcetera. First, fields in the unloaded structure are calculated, which means that there is no current flowing in the windings. These
Figure 4.8: Torque vs. speed characteristic initial design. P1 denotes the required torque at rated speed.

Figure 4.9: 3D drawing of initial design
4.4. FEM MODEL

Table 4.3: Initial sizes and quantities

<p>| | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_o$</td>
<td>98.7mm</td>
</tr>
<tr>
<td>$D_{gap}$</td>
<td>86.1mm</td>
</tr>
<tr>
<td>$h_m$</td>
<td>3.0mm</td>
</tr>
<tr>
<td>$h_{bi}$</td>
<td>3.1mm</td>
</tr>
<tr>
<td>$w_t$</td>
<td>4.4mm</td>
</tr>
<tr>
<td>$h_t$</td>
<td>13.3mm</td>
</tr>
<tr>
<td>$N$</td>
<td>199</td>
</tr>
</tbody>
</table>

Table 4.4: Realised loadings

<table>
<thead>
<tr>
<th></th>
<th>Initial guess</th>
<th>realised</th>
</tr>
</thead>
<tbody>
<tr>
<td>Magnetic loading</td>
<td>0.9T</td>
<td>0.65T</td>
</tr>
<tr>
<td>Electrical loading</td>
<td>10kA/m</td>
<td>11.7kA/m</td>
</tr>
</tbody>
</table>

Results are given in Fig. 4.10 and 4.11. Notice the dip in the airgap flux density, which comes from the space between two adjacent poleshoes.

It can be seen that the estimated flux densities correspond quite well with the FEM results. The teeth flux density is staying under 1.7 Tesla. The flux density in the magnets is equal to 1.10 T, which is the same as the predicted value from the permeance coefficient. The average airgap flux density is 0.71 T, where a value of 0.65 T was expected. This means that the leakage factor which was estimated to be 0.85 is actually slightly higher:

$$f_{lkg} = \frac{B_{gap}}{B_{m0}} = 0.9$$  \hspace{1cm} (4.31)

In future calculations this number should be used.

In FEM, it is also possible to calculate the torque of the motor (as function of the position). When this is done, the result of Fig. 4.12 are obtained. The RMS value of the torque is found to be equal to 0.68 Nm, while the initial design procedure estimated it to be 0.62 Nm. Part of this difference is due to error made in estimating the leakage factor. Based on this results it can be concluded that the initial design rules and calculations can already give an rough estimate of the torque produced by the motor.
Figure 4.10: Flux density distribution and B vectors inside initial design
Figure 4.11: Flux density distribution inside initial design: Airgap (top), Magnets (center) and teeth (bottom)
Figure 4.12: Torque vs electrical angle no commutation (left) and six step commutation (right) over a single pole pitch
4.4. FEM MODEL

Now that the sizing equations were validated and the calculated value of magnetic loading ($B_{gap}$) was shown to be ok, it is possible to return to these equations and use them iteratively to come to a design which comes to the torque requirements. Each time when the magnetic and electrical loading are calculated the initial guessed values are updated. In this way the diameter of the machine will be changed according to the realised loadings, resulting in a machine with the proper dimensions. The results are given in Table 4.5. A plot of the initial estimated torque vs. speed characteristic is shown in Fig. 4.13.

![Figure 4.13: Torque vs. speed characteristic iterative initial design. P1 denotes the required torque at rated speed.](image)

Table 4.5: Initial sizes and quantities (iterative sizing)

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_o$</td>
<td>115.9mm</td>
</tr>
<tr>
<td>$D_{gap}$</td>
<td>102.4mm</td>
</tr>
<tr>
<td>$h_m$</td>
<td>3.2mm</td>
</tr>
<tr>
<td>$h_{bi}$</td>
<td>3.6mm</td>
</tr>
<tr>
<td>$w_t$</td>
<td>5.5mm</td>
</tr>
<tr>
<td>$h_t$</td>
<td>16.9mm</td>
</tr>
<tr>
<td>$N$</td>
<td>218</td>
</tr>
</tbody>
</table>

The FEM results for this design are given in Fig. 4.14 and 4.15. When the numbers from the initial sizing and FEM are compared, Table 4.6 is obtained. As can be seen, the developed torque is very close to the required torque to realise 15N pull force. The actual developed pull force is 14.5N. However, to realise this torque, a motor with a diameter of 116mm was required, which is too large to fit in the feeders. Later on, in chapter 8 when other design options are evaluated, a motor is designed which does fit in the volume.

At this point it can be concluded that the derived set of sizing equations is ok. The expected values of torque come very close to the results from FEM analysis. The set of equations is capable of sizing the motor such that the developed torque is very close to the required value. This is very helpful when one needs to quickly create a motor design and give some initial guesses on its performance.
Figure 4.14: Flux density distribution and B vectors inside iterative initial design

Table 4.6: Iterative design, comparison between initial estimates and FEM

<table>
<thead>
<tr>
<th></th>
<th>Initial estimate</th>
<th>FEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Airgap flux density</td>
<td>0.6931T</td>
<td>0.6963T</td>
</tr>
<tr>
<td>Teeth flux density</td>
<td>1.70T</td>
<td>1.66T</td>
</tr>
<tr>
<td>Torque</td>
<td>0.8397Nm</td>
<td>0.8505Nm</td>
</tr>
</tbody>
</table>
Figure 4.15: Flux density distribution inside iterative design: Airgap (top left), Magnets (top right) and teeth (bottom)
Chapter 5

Optimizing the design

In the previous section, a motor was designed using initial sizing rules. But in order to use as less space as possible, it needs to be sure that this design is optimal. In order to come to an optimal design rapidly, a Magnetic Equivalent Circuit model was created, which enables very quick performance calculations on the design. The MEC model is validated with the FEM calculations of the initial designs. When the model fits the data of the FEM calculations, it is used to come to an improved design.

5.1 MEC model

In order to get more insight in the limitations of the design a Magnetic Equivalent Circuit model is derived. This model gives the possibility to get performance estimates from very quick calculations.

The creation of the MEC model is started with drawing the equivalent circuit of the magnetic circuit [9]. Figure 5.1 contains a segment of a typical BLDC outer rotor motor. From this drawing it is very straightforward to derive the MEC circuit. A segment of this is given in figure 5.2. An overview of the reluctances in the network and their names is given in table 5.1.

<table>
<thead>
<tr>
<th>$R_{m,i}$</th>
<th>Reluctance of the $i^{th}$ permanent magnet</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R_{ml,i}$</td>
<td>Magnet Leakage reluctance</td>
</tr>
<tr>
<td>$R_{g,i,j}$</td>
<td>Airgap reluctance between $i^{th}$ magnet and $j^{th}$ tooth</td>
</tr>
<tr>
<td>$R_{t,j}$</td>
<td>Reluctance of the $j^{th}$ tooth</td>
</tr>
<tr>
<td>$R_{l,j}$</td>
<td>Teeth leakage reluctance</td>
</tr>
<tr>
<td>$R_{s,j}$</td>
<td>Reluctance of the stator yoke</td>
</tr>
<tr>
<td>$R_{bi,i}$</td>
<td>Reluctance of the rotor back iron</td>
</tr>
</tbody>
</table>

Off coarse all the component values depend on both the magnet properties of the materials and the geometrical dimensions of each part. Therefore an overview of these variables is given in table 5.2 and table 5.3.
Figure 5.1: Part of the BLDC geometry

Figure 5.2: Corresponding Magnetic Equivalent Circuit
Table 5.2: input variables: magnet properties

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\mu_0$</td>
<td>Magnetic permeability of vacuum</td>
</tr>
<tr>
<td>$\mu_{r,bi,i}$</td>
<td>Relative magnetic permeability of the rotor back-iron</td>
</tr>
<tr>
<td>$\mu_{r,t,j}$</td>
<td>Relative magnetic permeability of the $j^{th}$ tooth</td>
</tr>
<tr>
<td>$\mu_{r,sc,j}$</td>
<td>Relative magnetic permeability of the stator yoke</td>
</tr>
<tr>
<td>$\mu_{r,pm}$</td>
<td>Relative magnetic permeability of the magnet material</td>
</tr>
<tr>
<td>$H_{cb}$</td>
<td>$H_{cb}$ of magnet material</td>
</tr>
</tbody>
</table>

Table 5.3: Input variables: geometrical properties

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_{so}$</td>
<td>Stator outer diameter</td>
</tr>
<tr>
<td>$R_{so}$</td>
<td>Stator outer radius</td>
</tr>
<tr>
<td>$D_{ro}$</td>
<td>Rotor outer diameter</td>
</tr>
<tr>
<td>$R_{ro}$</td>
<td>Rotor outer radius</td>
</tr>
<tr>
<td>$l$</td>
<td>Axial (effective) length of the motor</td>
</tr>
<tr>
<td>$g$</td>
<td>Height of the airgap</td>
</tr>
<tr>
<td>$p$</td>
<td>Number of magnet poles on the rotor</td>
</tr>
<tr>
<td>embrace</td>
<td>Magnetic pole embrace</td>
</tr>
<tr>
<td>$h_m$</td>
<td>Magnet height</td>
</tr>
<tr>
<td>$o_{magnet}$</td>
<td>Opening width between adjacent magnets</td>
</tr>
<tr>
<td>$N_p$</td>
<td>Number of stator slots</td>
</tr>
<tr>
<td>$h_t$</td>
<td>Height of the teeth</td>
</tr>
<tr>
<td>teeth ratio</td>
<td>Fraction teeth width / slot pitch</td>
</tr>
<tr>
<td>$w_{shoe}$</td>
<td>Width of the pole shoe</td>
</tr>
<tr>
<td>$h_{shoe}$</td>
<td>Thickness of the pole shoe</td>
</tr>
<tr>
<td>$o_{slot}$</td>
<td>Opening width between two adjacent pole shoes.</td>
</tr>
</tbody>
</table>

Table 5.4: Calculated geometrical variables

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_{ri}$</td>
<td>$D_{so} + 2g$</td>
</tr>
<tr>
<td>$R_{ri}$</td>
<td>Rotor inner diameter</td>
</tr>
<tr>
<td>$w_m$</td>
<td>$\mu_{Dca} \cdot 2g$</td>
</tr>
<tr>
<td>$\lambda_p$</td>
<td>Width of a magnet</td>
</tr>
<tr>
<td>$\lambda_s$</td>
<td>Magnetic pole pitch</td>
</tr>
<tr>
<td>$\lambda_s$</td>
<td>Slot pitch</td>
</tr>
<tr>
<td>$w_t$</td>
<td>$\mu_{Dca} \cdot \lambda_s$</td>
</tr>
<tr>
<td>$h_{bi}$</td>
<td>$R_{ro} - R_{ri} - h_m$</td>
</tr>
<tr>
<td>Height of the back iron</td>
<td></td>
</tr>
</tbody>
</table>

5.1.1 Reluctance calculations

When all geometrical variables are known, all component values of the magnetic equivalent circuit can be calculated. Equations for these values are given below:

**Back iron reluctance:**

$$ R_{bi,i} = \frac{\lambda_p}{\mu_0 \mu_{r,bi,i} h_{bi} l} $$

**Magnet reluctance:**
5.1. MEC MODEL

Airgap reluctance:
Calculation of the airgap reluctances / permeances is more complicated, since these are dependent on the position of the rotor. An expression to approximate airgap permeances as a function of the position for slotted structures can be found in Ostovic [9]. The expressions are given below:

\[ R_m = \frac{h_m}{\mu_r \mu_0 w_m l} \] (5.2)

\[ G_{\text{max}} = \mu_0 \frac{lw_{\text{min}}}{g} \] (5.3)

\[ w_{\text{min}} = \min (w_{\text{shoe}}, w_m) \] (5.4)

\[ D_{\text{gap}} = \frac{D_{\text{so}} + D_{\text{ri}}}{2} \] (5.5)

\[ y_t' = \frac{|w_{\text{teeth}} - w_m|}{D_{\text{gap}}} \] (5.6)

\[ y_t = \frac{w_m + w_{\text{shoe}} + o_{\text{slot}} + o_{\text{magnet}}}{D_{\text{gap}}} \] (5.7)
\[ G_{11}(y) = \begin{cases} 
G_{\text{max}} & \text{for } 0 \leq y \leq y_t' \text{ and } 2\pi - y_t' \leq y \leq 2\pi \\
\frac{G_{\text{max}}}{1 + \cos\left(\frac{\pi (y - y_t')}{y_t - y_t'}\right)} & \text{for } y_t' \leq y \leq y_t \\
\frac{G_{\text{max}}}{1 + \cos\left(\frac{\pi (y - 2\pi + y_t')}{y_t - y_t'}\right)} & \text{for } 2\pi - y_t \leq y \leq 2\pi - y_t' \\
0 & \text{for } y_t \leq y \leq 2\pi - y_t 
\end{cases} \] (5.8)

In these expressions, \( y \) is the displacement angle, \( \alpha_{\text{slot}} \) is the opening between two neighboring slots, and \( \alpha_{\text{magnet}} \) is the opening between two neighboring magnets. \( G_{\text{max}} \) is the corresponding peak value of the gap permeance. The resulting permeance as a function of the rotor position is plotted in Fig. 5.4. This is done for a single (full) rotation of the rotor part, thus not for a single pole pitch or slot pitch, since it describes the permeance between one particular magnet and one particular tooth, which are opposite to each other only ones per revolution.

![Figure 5.4: Airgap Permeance vs angle (right: close-up)](image)

**Teeth reluctance:**
The reluctance of the teeth are quite straightforward to calculate. First it is assumed that the teeth are rectangular, having the same width \( (w_t) \) over their entire length:

\[ R_{t,j} = \frac{h_t}{\mu_0 \mu_{t,j} w_t l}. \] (5.9)

**Stator core reluctance:**

\[ R_{sc} = \frac{\pi (R_{so} - h_t) / N_s}{\mu_0 \mu_{r,sc} l (R_{so} - h_t - R_{si})}. \] (5.10)

**Leakage reluctances:**
The model incorporates two types of leakages. The magnet leakage is the flux flowing from one magnet to the neighbouring magnet, without crossing the gap and therefore no part of the flux linkage. The reluctance of this path is estimated:

\[ R_{ml} = \frac{\alpha_{\text{magnet}}}{\mu_0 h_m}. \] (5.11)
5.1. MEC MODEL

The second type of leakage is the leakage between two neighbouring pole shoes. When the opening between two shoes decreases, the leaking flux increases. Usually it is preferable to keep the openings very small in order to minimize cogging torque, hence it is important to take this leakage into account. The reluctance representing this leakage is estimated:

\[ R_{sl} = \frac{a_{slot}}{\mu_0 l_{shoe}}. \]  

(5.12)

5.1.2 MMF forces

After all reluctances of the network are calculated, the remaining unknowns in the circuit are the mmf sources. These are rather straightforward to calculate:

\[ F_{m,i} = (-1)^i H_{cb} h_{m} \]  

(5.13)

\[ F_{coil,j} = NI_{coil,j} \]  

(5.14)

Or in vector notation:

\[ F_m = \begin{pmatrix} 1 & -1 & 1 & \ldots & -1 \end{pmatrix} H_{cb} h_{m} \]  

(5.15)

the matrix with ones with alternating signs is just to interleave north and south poles, and thus directions of the mmf sources.

\[ F_{coil} = NKI = N \begin{pmatrix} 1 & 0 & 0 \\ 0 & 1 & 0 \\ 0 & -1 & 0 \\ \vdots & \vdots & \vdots \\ -1 & 0 & 0 \end{pmatrix} \begin{pmatrix} I_a \\ I_b \\ I_c \end{pmatrix} \]  

(5.16)

in which K is the matrix which describes the winding layout. It describes which phase is wound around a particular tooth, and the sign describes the direction of winding. Therefore K is a matrix which dimensions are \( N_s \times 3 \).

5.1.3 MEC Solution Procedure

Solving the magnetic equivalent circuit means to find the flux in all branches along with the magnetic scalar potentials of all nodes, for the previous derived set of magnetomotive forces and permeances. Due to the dualities between magnetic and electric fields, this is analog to a solving a DC electric circuit with sources and/or linear or nonlinear resistances only. Hence, the state of the magnetic circuit is completely described by a set of algebraic equations. The universality of Kirchhoff’s laws, and the circuit solution methods based upon them, allows these solution methods to be used in the solution of magnetic equivalent circuits. Of particular interest here is the node potential method, because of its simple way of handling infinite reluctances, which often appear in air gap representations.

5.1.4 Node Potential Equations and Permeance Matrices

In the node potential technique, known values of currents are written on the right-hand sides of the equations. When this method is employed in the magnetic equivalent circuit procedure, known values of fluxes will appear on the right-hand side of the equations.
First the scalar magnetic potentials are defined as the elements of four vectors:

\[
\begin{align*}
\mathbf{u}_1 &= \begin{pmatrix} u_{1,1} & u_{1,2} & \ldots & u_{1,i} & u_{1,i+1} & \ldots & u_{1,p} \end{pmatrix}^T, \\
\mathbf{u}_2 &= \begin{pmatrix} u_{2,1} & u_{2,2} & \ldots & u_{2,i} & u_{2,i+1} & \ldots & u_{2,p} \end{pmatrix}^T, \\
\mathbf{u}_3 &= \begin{pmatrix} u_{3,1} & u_{3,2} & \ldots & u_{3,j} & u_{3,j+1} & \ldots & u_{3,N_s} \end{pmatrix}^T, \\
\mathbf{u}_4 &= \begin{pmatrix} u_{4,1} & u_{4,2} & \ldots & u_{4,j} & u_{4,j+1} & \ldots & u_{4,N_s-1} \end{pmatrix}^T.
\end{align*}
\]

Note, that while there are \( N_s \) nodes at the stator level, \( \mathbf{u}_4 \) has only \( N_s - 1 \) elements. This comes from the fact that it is needed to set a reference node, which is specified as:

\[
u_{4,N_s} = 0
\]

Using the Kirchhoff current laws, one can derive the node potential equations for each node:

\[
(\mathbf{u}_{1,1} - \mathbf{u}_{1,p}) G_{ry,1} + (\mathbf{u}_{1,1} - \mathbf{u}_{1,2}) G_{ry,2} = \phi_{r,1}
\]

which can be rewritten into:

\[
u_{1,1} (G_{ry,1} + G_{ry,2}) - u_{1,p} G_{ry,1} - u_{1,2} G_{ry,2} = -\phi_{r,1}
\]

For the nodes belonging to \( \mathbf{u}_1 \) this results in:

\[
\begin{align*}
u_{1,1} (G_{ry,1} + G_{ry,2}) - u_{1,p} G_{ry,1} - u_{1,2} G_{ry,2} &= -\phi_{r,1} \\
u_{1,2} (G_{ry,2} + G_{ry,3}) - u_{1,1} G_{ry,2} - u_{1,3} G_{ry,3} &= -\phi_{r,2} \\
u_{1,3} (G_{ry,3} + G_{ry,4}) - u_{1,2} G_{ry,3} - u_{1,4} G_{ry,4} &= -\phi_{r,3} \\
\vdots & \vdots \\
u_{1,p} (G_{ry,p} + G_{ry,1}) - u_{1,p-1} G_{ry,p} - u_{1,1} G_{ry,1} &= -\phi_{r,p}
\end{align*}
\]

which can again be written in a matrix notation:

\[
A_{11} = \begin{pmatrix} G_{ry,1} + G_{ry,2} & -G_{ry,2} & 0 & \ldots & 0 & 0 & -G_{ry,1} \\
-G_{ry,2} & G_{ry,2} + G_{ry,3} & -G_{ry,3} & \ldots & 0 & 0 & \vdots \\
-\cdots & \vdots & \vdots & \ddots & \vdots & \vdots & \vdots \\
-\cdots & \vdots & \vdots & \ddots & G_{ry,p} & G_{ry,p} + G_{ry,1} \end{pmatrix}
\]

\[
\phi_r = \begin{pmatrix} \phi_{r,1} & \phi_{r,2} & \cdots & \phi_{r,p} \end{pmatrix}^T
\]

\[
A_{11} \mathbf{u}_1 = -\phi_r.
\]

The same can be done for the nodes belonging to \( \mathbf{u}_2 \), \( \mathbf{u}_3 \) and \( \mathbf{u}_4 \):

\[
\phi_s = \begin{pmatrix} \phi_{s,1} & \phi_{s,2} & \cdots & \phi_{s,N_s} \end{pmatrix}^T
\]

\[
\phi_s' = \begin{pmatrix} \phi_{s,1} & \phi_{s,2} & \cdots & \phi_{s,N_s-1} \end{pmatrix}^T
\]
\[ A_{22} = \begin{pmatrix}
2G_{ml} + \sum_{j=1}^{N_s} G_{gap,j} & -G_{ml} & 0 & \cdots & 0 & 0 & -G_{ml} \\
-G_{ml} & 2G_{ml} + \sum_{j=1}^{N_s} G_{gap,j} & -G_{ml} & \cdots & 0 & 0 & 0 \\
\vdots & \vdots & \vdots & \ddots & \vdots & \vdots & \vdots \\
-G_{ml} & 0 & 0 & \cdots & 0 & -G_{ml} & 2G_{ml} + \sum_{j=1}^{N_s} G_{gap,j}
\end{pmatrix} \]

(5.27)

\[ A_{23} = \begin{pmatrix}
G_{gap_{1,1}} & G_{gap_{1,2}} & \cdots & G_{gap_{1,N_s}} \\
G_{gap_{2,1}} & G_{gap_{2,2}} & \cdots & G_{gap_{2,N_s}} \\
\vdots & \vdots & \ddots & \vdots \\
G_{gap_{p,1}} & G_{gap_{p,2}} & \cdots & G_{gap_{p,N_s}}
\end{pmatrix} \]

(5.28)

\[ A_{32} = A_{23}^T \]

(5.29)

\[ A_{33} = \begin{pmatrix}
2G_{tl} + \sum_{j=1}^{p} G_{gap,j,1} & -G_{tl} & 0 & \cdots & 0 & 0 & -G_{tl} \\
-G_{tl} & 2G_{tl} + \sum_{j=1}^{p} G_{gap,j,2} & -G_{tl} & \cdots & 0 & 0 & 0 \\
\vdots & \vdots & \vdots & \ddots & \vdots & \vdots & \vdots \\
-G_{tl} & 0 & 0 & \cdots & 0 & -G_{tl} & 2G_{tl} + \sum_{j=1}^{p} G_{gap,j,12}
\end{pmatrix} \]

(5.30)

\[ A_{44} = \begin{pmatrix}
G_{sy,1} + G_{sy,2} & -G_{sy,2} & 0 & \cdots & 0 & 0 & 0 \\
-G_{sy,2} & G_{sy,2} + G_{sy,3} & -G_{sy,3} & \cdots & 0 & 0 & 0 \\
\vdots & \vdots & \vdots & \ddots & \vdots & \vdots & \vdots \\
0 & 0 & 0 & \cdots & 0 & -G_{sy,N_s-1} & G_{sy,N_s-1} + G_{sy,N_s}
\end{pmatrix} \]

(5.31)

\[ A_{11} u_1 = -\phi_r, \quad (5.32) \]
\[ A_{22} u_2 + A_{23} u_3 = \phi_r, \quad (5.33) \]
\[ A_{32} u_2 + A_{33} u_3 = -\phi_s, \quad (5.34) \]
\[ A_{44} u_4 = \phi_s' \quad (5.35) \]

or

\[ \begin{bmatrix}
A_{11} & 0 & 0 & 0 \\
0 & A_{22} & A_{23} & 0 \\
0 & A_{32} & A_{33} & 0 \\
0 & 0 & 0 & A_{44}
\end{bmatrix} \begin{bmatrix}
u_1 \\
u_2 \\
u_3 \\
u_4
\end{bmatrix} = \begin{bmatrix}
-\phi_r \\
\phi_r \\
-\phi_s \\
\phi_s'
\end{bmatrix}. \quad (5.36)\]
Note, that not all relationships in the circuit are described by the node potential equations. Branches like the one shown in Fig. 5.5 do not appear in these equations. To complete the set of algebraic equations, Ampere’s law is used to describe the relation between \( u_a \) and \( u_b \). For the branch in Fig. 5.5 this is:

\[
 u_a = u_b - \phi R + F .
\]  

(5.37)

For the MEC of Fig. 5.2 this yields:

\[
 u_{1,1} = u_{2,1} + F_{m,1} + \phi_{r,1} , \\
 u_{1,2} = u_{2,2} + F_{m,2} + \phi_{r,2} , \\
 \dot{\ldots} , \\
 u_{1,p} = u_{2,p} + F_{m,p} + \phi_{r,p} ,
\]

(5.38)

which can again be written in matrix form:

\[
 u_1 = u_2 + E_m + R_m \phi_r ,
\]

(5.39)

with

\[
 R_m = \text{diag} \left( R_{m,1}, R_{m,2}, \ldots, R_{m,p} \right) .
\]

(5.40)

The same is done for the stator part, which can be written as:

\[
 u_3 = I_{N_s, N_s-1} u_t + E_{\text{coil}} + R_t \phi_s 
\]

(5.41)

with \( I_{N_s, N_s-1} \) an identity matrix with \( N_s \) rows and \( N_s - 1 \) columns (having the final row all zeros), and

\[
 R_t = \text{diag} \left( R_{t,1}, R_{t,2}, \ldots, R_{t,N_s} \right) .
\]

(5.42)

Combining these equations with the earlier derived equations for the mmf sources and the
5.1. MEC MODEL

node potential equations, results in a full set of equations which describe the MEC model:

\[
\begin{bmatrix}
A_{11} & 0 & 0 & 0 & I_{p,p} & 0 & 0 \\
0 & A_{22} & A_{23} & 0 & -I_{p,p} & 0 & 0 \\
0 & A_{32} & A_{33} & 0 & 0 & I_{N_s,N_s} & 0 \\
0 & 0 & 0 & A_{44} & 0 & 0 & I_{N_s-1,N_s} \\
I_{p,p} & -I_{p,p} & 0 & 0 & -R_m & 0 & 0 \\
0 & 0 & I_{N_s,N_s} & -I_{N_s,N_s-1} & 0 & -R_s & 0 \\
\end{bmatrix}
\begin{bmatrix}
u_1 \\
u_2 \\
u_3 \\
u_4 \\
\phi_p \\
\phi_s \\
E_m \\
E_s \\
\end{bmatrix} = \begin{bmatrix}
0 \\
0 \\
0 \\
0 \\
0 \\
0 \\
F_m \\
F_s \\
\end{bmatrix}
\]

(5.43)

The set of unknown fluxes and node potentials can very easily be calculated from the known mmf-sources, by matrix inversion:

\[
Ax = y \Rightarrow x = A'y
\]

(5.44)

When the node potentials are known, it is straightforward to calculate fluxes and flux densities. For the back-iron this results in:

\[
B_{bi,1} = \frac{\left(u_{1,1} - u_{1,p}\right)}{h_{bi}} G_{bi,1},
\]

\[
B_{bi,2} = \frac{\left(u_{1,2} - u_{1,1}\right)}{h_{bi}} G_{bi,2},
\]

\[
\vdots
\]

\[
B_{bi,p} = \frac{\left(u_{1,p} - u_{1,p-1}\right)}{h_{bi}} G_{bi,p}.
\]

(5.45)

Simlar for the stator core:

\[
B_{sc,1} = \frac{\left(u_{4,1} - u_{4,N_s}\right)}{l(R_{so} - h_t - R_{si})} G_{sc,1},
\]

\[
B_{sc,2} = \frac{\left(u_{4,2} - u_{4,1}\right)}{l(R_{so} - h_t - R_{si})} G_{sc,2},
\]

\[
u_{4,N_s} = 0.
\]

(5.46)

And for the stator teeth:

\[
B_{t,1} = \frac{\phi_{s,1}}{u_{t,l}},
\]

\[
B_{t,2} = \frac{\phi_{s,2}}{u_{t,l}},
\]

\[
\vdots
\]

\[
B_{t,N_s} = \frac{\phi_{s,N_s}}{u_{t,l}}.
\]

(5.47)

5.1.5 Including saturation effects

When flux densities in the iron parts rise above 1.7 T, the iron part can no longer be seen as linear part with constant permeability. With higher values of B, permeability drops and reluctance of iron parts increase significantly. While in the unsaturated case these reluctances were small compared to those of the airgap and magnets, with higher values of B they can become the dominant factor in the reluctance path. Therefore an iterative loop is integrated, which recalculates permeability of the iron parts for the current value of flux densities. These are then used in the next step.
5.1.6 Torque production

The produced torque is calculated by evaluating the energy in the circuit. The magnetic energy ($W_m$) and co-energy ($W'_m$) in the circuit are equal to [3]:

$$W_m = \int_V \left[ \int_0^B H dB \right] dV$$

$$W'_m = \int_V \left[ \int_0^H B dB \right] dV$$

For linear magnetic systems it can be found that this is equal to:

$$W_m = \frac{1}{2} \int_V B H dV$$

$$W'_m = \sum_{source} \frac{1}{2} \phi_{source} F_{source}$$

This is illustrated in Fig. 5.7. For the circuit of Fig. 5.2 this is equal to:

$$\sum_{i=1}^{p} \frac{1}{2} \phi_{t,i} F_{m,t} + \sum_{j=1}^{N_s} \frac{1}{2} \phi_{s,j} F_{col,j}$$

For non-linear systems this is more complicated. In that case the energy is obtained by summing the energies stored in all passive elements of the magnetic equivalent circuit [9]:

$$W_m = \sum_{j=1}^{n} W_{m,n} = \sum_{j=1}^{n} \left( \int_{V_n} \left[ \int_0^{B_n} H_n dB \right] dV \right)$$

$$W_m = \sum_{j=1}^{n} \left( V_n \int_0^{B_n} H_n dB \right)$$
From the law of energy conservation it can be derived that:

\[ T = -\frac{\delta W_m}{\delta \theta} = \frac{\delta W'_m}{\delta \theta} \] (5.55)

Another method for evaluating the developed torque is using the flux linkage. The flux linkage is equal to the flux through a coil, times the number of conductors. The torque is now calculated via:

\[ T = I_a \frac{\delta \lambda_a}{\delta \theta} + I_b \frac{\delta \lambda_b}{\delta \theta} + I_c \frac{\delta \lambda_c}{\delta \theta} \] (5.56)

\[ \lambda_a = NK(n,1)\phi_s, \quad \lambda_b = NK(n,2)\phi_s, \quad \lambda_c = NK(n,3)\phi_s \] (5.57)

in which \( K(n,1) \) denotes the first column in the winding matrix \( K \) and \( \phi_s \) is the vector with all teeth fluxes. Since there is no need to integrate along the BH-curve for each element, it is much easier to calculate.

### 5.1.7 Electrical equations

The electrical equations relating geometrical dimensions to electrical quantities and the power source were already used in the initial sizing method, and are only repeated here for completeness:

\[ I(\omega_m) = \min \left( I_{\max}, \frac{V_{\max} - E(\omega_m)}{R_{ph-ph}} \right) \] (5.58)

\[ R_{ph-ph} = \frac{2}{3} Z \frac{l_t}{2 \sigma A_{wire}} = \frac{2}{3} Z^2 \frac{l_t}{2 \sigma k_f A_{slots}} \] (5.59)

\[ E(\omega_m) = k_e \omega_m \] (5.60)

\[ k_e = \frac{(2/3)k_w \cdot Z \cdot \phi_{m0} \cdot p}{\pi} \] (5.61)
5.1.8 Model validation

Validating the model is done by comparing the results obtained by implementing all equations in matlab with the results of the FEM models of the designs from previous chapter. When this is done for the first design from the previous chapter, the torque characteristics from Figure 5.8 are the result.

It can be seen that the MEC model is underestimating the torque capability of the motor by roughly 12%. This is most likely due to the fact that the leakage between the magnets is over estimated, resulting in less flux crossing the gap. When a plot of the teeth flux density vs position is created, it can be seen that in the MEC model the peak value is only 1.5T, instead of the 1.7T which it should be.

The same is done for the second derived motor from previous chapter, resulting in a similar plot (Fig. 5.10). Note that to save time the FEM calculation was done for only 10 positions, resulting in some error. (updated plot with 20 points will be added). Again, the MEC model underestimates the developed torque, this time by 7%. Using this model it is already possible to see the influence of the design variables on the torque, therefore the model is not further adjusted. The small error in absolute values for torque and teeth flux are accepted.

5.1.9 Optimization routine

With the previous derived MEC model, one can calculate output torques for different designs very quickly. Thus, having the initial sizing rules, it is possible to generate various motor designs for varying diameters and varying power supplies and calculate initial performance estimates. But in order to be sure that the developed torque from the volume is maximal, an optimization routine was written which optimizes the dimensioning of the iron parts, such
5.1. **MEC MODEL**

Figure 5.9: MEC model teeth flux density vs rotor position

Figure 5.10: Comparison FEM and MEC model (iterative derived design) left: uncommutated, right: six step commutation
that the output torque is maximum. When the initial sizing rules are correct the benefit from this routine will be small, but when initial estimates are wrong one can have a significant increase in torque from this method.

**Routine**

The routine optimizes magnet height, teethratio, wirediameter and slot opening, for a fixed diameter such that the output torque is maximized. In order to do so, one needs to have an initial estimate for these variables. These are the sizes which come from the initial sizing rules. After that, it iteratively changes the parameters, and evaluates the produced torque using the non-linear MEC model. Using the linear MEC model would speed up the routine significantly but results in wrong sizes for the iron parts, since in that case those would not saturate, and optimization routine will minimize the iron (and thus maximize the area for the copper windings). An illustration of this routine and the iterative steps is given in Fig. 5.11. When initial sizing are well defined, the torque increase from this routine is very small. However, if something was wrong in the sizing method, this routine comes up with the dimensions giving more torque. Therefore this routine can also be used to verify the initial sizing methods.
5.1.10 Conclusion

During this chapter, a MEC model was created as a tool for fast performance calculations of a motor design. It is able to estimate the torque produced by the motor, giving the opportunity to quickly evaluate the impact of several design variables. Furthermore, an optimization routine was written, which is able to adjust geometrical dimensions to maximize torque. Using this routine it is possible to check the earlier derived sizing rules, and/or improve the design from this rules.
Chapter 6

Sensor Possibilities

6.1 Requirements

High diameter direct drive motors require a very accurate sensing system in order to meet the accuracy requirements of the feeding system. As stated in chapter 2, minimum static placement accuracy is $40\mu m$. This means that sensor resolution needs to be in the range of several $\mu m$s. This can be translated to angular resolutions depending on the diameter:

<table>
<thead>
<tr>
<th>Diameter</th>
<th>$40\mu m$</th>
<th>$10\mu m$</th>
</tr>
</thead>
<tbody>
<tr>
<td>50mm</td>
<td>$3925 \approx 0.092^\circ$</td>
<td>$15700 \approx 0.022^\circ$</td>
</tr>
<tr>
<td>60mm</td>
<td>$4710 \approx 0.076^\circ$</td>
<td>$18840 \approx 0.019^\circ$</td>
</tr>
<tr>
<td>70mm</td>
<td>$5495 \approx 0.066^\circ$</td>
<td>$21980 \approx 0.016^\circ$</td>
</tr>
<tr>
<td>80mm</td>
<td>$6280 \approx 0.057^\circ$</td>
<td>$25120 \approx 0.014^\circ$</td>
</tr>
<tr>
<td>90mm</td>
<td>$7065 \approx 0.051^\circ$</td>
<td>$28260 \approx 0.013^\circ$</td>
</tr>
<tr>
<td>100mm</td>
<td>$7854 \approx 0.046^\circ$</td>
<td>$31416 \approx 0.012^\circ$</td>
</tr>
</tbody>
</table>

Table 6.1: Required angular resolution (CPR)

6.2 Possibilities

There are several concepts for position detection in actuator systems, the most promising will be briefly discussed. The most important factor for selecting possibilities is the cost, since most high resolution solutions are expensive. A list of possible candidates:

- Hall-effect sensors
- MR sensors
- Optical encoders (also used on Lightscribe DVDs)
- Philips Twin Laser (as used in optical computer mice)

6.2.1 Hall-effect sensors

Hall-effect measure the strength of the magnet field in its vicinity, and convert this to an analog output voltage (using the Hall-effect). In a hall-effect sensor, a current is flowing
through a conductor. A magnet field in the vicinity deflects the path of the moving electrons, resulting in a difference in potential between the sides of the conductor. Usually the sensors are placed underneath the rotating rotor, measuring the field from the 10 pole pairs. This automatically means that this not an absolute position measurement. When the rotor rotates, the output of the hall-sensor is (ideally) a sinewave. This sinewave can than be interpolated to come to a position measurement. One can use a second hall-sensor displaced over 90° to get sine/cosine signalling, and recover the angle:

$$\theta = \tan^{-1}\left(\frac{V_o}{V_b}\right)$$  \hspace{1cm} (6.1)

The accuracy of such a solution depends heavily on the shape of the magnet field produced by the magnets. If we assume that the field is perfectly sinusoidal, and assume that interpolation of a factor 1000 is possible, the resolution will be 10,000 per revolution (Assuming that there are 10 pole pairs on the rotor) or:

$$\text{resolution} = \pi \cdot \frac{D_o}{\frac{1000}{2}}$$  \hspace{1cm} (6.2)

This is equal to 29.8μm for the previously derived motor. This is already in the range of the required resolution, thus might be a feasible solution. However since the the field has to be very good sinusoidal for high resolutions, using the magnets on the rotor is impossible.

### 6.2.2 MR sensors

Another kind of sensors using magnetic effects are the MR (Magneto-Resistive) sensors, which use the magneto-resistive effect. This is the effect that for some materials the electrical resistance changes with the angle between the orientation of the magnet field and the direction of the current. When this type of sensor is used, one places an additional (very small magnet) on the rotating axis, which then rotates on top of the sensor IC. Typical resolutions are 0.05°, thus already close to the required resolution here. (For example the Honeywell IMC-1512: [www.position-sensors.com](http://www.position-sensors.com)) Note that this is the stated resolution of the sensor, which is
CHAPTER 6. SENSOR POSSIBILITIES

Figure 6.2: Optical encoder used in Lightscribe technology

typically higher than the absolute accuracy of the sensors. For example the philips / NXP KMA200 states a resolution of 0.022° when the digital output is used, but the digital noise level is 4LSB, resulting in an accuracy of 0.088°. For the analog output a resolution of 0.05° is stated, with an output noise of 0.1° (www.nxp.com). Furthermore it must be noted that most MR sensors have a angular range of 180°, thus an aditional sensor, for example a very simple hall element, is required to get a full position measurement. There are also MR sensors having the full 360° range on the market, for example the Melexis 90316. This states a 10bit angular accuracy, which corresponds to 0.36°. This IC has an angular resolution of 12bit, which equals 0.09°.

6.2.3 Optical encoders

Another solution which was handed to us, are optical encoders which are used on lightscribe DVDs. Each of these dvds has an optical reflective encoder pattern on the bottom side. Each dvd writer capable of writing lightscribe discs, has an optical sensor to read this pattern. Since this is a typical high volume application, the total costs of such a system must be very low. That is why it seems interesting. But this technique is limited to a resolution of 180LPI (lines per inch) or 7.09lines per mm. This means a resolution of 140μm, which is not sufficient for our application (www.avagotech.com). Since these IC's output a digital signal (reflecting or not reflecting) interpolation is not possible. When optical sensors with an analog output (sinusoidal) are used, interpolation might be possible to come to a sufficiently accurate measurement.

6.2.4 Philips Twin Laser Technology

The final option which is discussed is the philips twin laser technology. This is the same technology which is in the optical laser mice which are on the market. It uses the same technique as laser methods with glass scales, but in stead of having this ideal scales the reflecting underground is used. The sensors on the market have an accuracy of 1%, which would be 20μm for a 2mm index action. The next generation of the chips incorporating
6.3. CONCLUSION

this technology will have an accuracy of 0.1\% which will be more than sufficient for the feeding application. Currently, work is being done to implement this technology also in paper transport systems for printers/copiers, but there are some difficulties. Using it in feeder applications will be no problem, when the application in copiers is possible. Advantage of this technique that it can be used to measure displacement directly on the tape, so that the backlash between tape and sprocketeeth is no longer an issue. Therefore this seems to be an interesting technique, but it might still take some years to become useable. Notice that this technique only measures relative displacements from the starting position. More on this technology can be found at the Philips website.

6.3 Conclusion

Getting a low cost sensor system with a sufficient high accuracy is difficult. Industry standards are becoming more and more accurate, and approach the required resolution. Currently, the MR sensors look the most promising, since these already have a very high resolution. Hall-sensors might also be an option, however high resolutions are only possible with very good sinusoidal fields. However, the twin laser technology mentioned before seems to have potential as well, with the advantage of measuring directly on the tape. It is interesting to follow the developments in this area, to see when it comes available.
Chapter 7

Production costs

Now that there is an initial design and a method to estimate its performance, a cost analysis is carried out. Doing this analysis shows which design choices influence the cost price. At the end of this chapter some options are given, which can reduce cost price significantly. In the next chapter their impact on performance is evaluated.

7.1 Initial estimation

In this section all separate parts of the motor are listed and briefly discussed, and an estimate of their production costs is given. The picture of the spindle motor (Fig. 7.1 also gives a good view on all parts which are required for such a drive solution. The initial estimate was just a coarse estimate of the costs of the used materials together with iron machining. This does not include costs for creating tooling, factory development, logistics and commercial profits.

7.1.1 Stator parts

The stator core is made of stamped iron sheets, which are pressed or glued together and afterwards covered in a insulating layer of vernis / epoxy. Additional poleshoes are pressed separately, and are placed on the stator part after the windings are placed on the teeth. The

Figure 7.1: DVD drive spindle motor
7.1. INITIAL ESTIMATION

coils are all identical and are wound around a mold. They can easily be shifted over the teeth, before the motor is assembled.

<table>
<thead>
<tr>
<th>Table 7.1: Cost stator parts</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator core</td>
</tr>
<tr>
<td>Winding Coils</td>
</tr>
<tr>
<td>Wire</td>
</tr>
<tr>
<td>Additional teeth</td>
</tr>
<tr>
<td>Total:</td>
</tr>
</tbody>
</table>

7.1.2 Rotor part

The rotor consists of a iron cup, which can be made using the deep drawing technique, and 20 permanent magnets which are glued on the inside. The price for the permanent magnets comes from a quotation at Bakker magnetics. Typical magnets of 10mm x 7mm x 2mm of BM-35 material, cost €0.06 to €0.10 each at the moment. But due to high market demands for neodymium prices are fluctuating very much at the moment. It is very well possible that prices are doubled within one year, but they could drop as well. This rotor cup is then placed on an axle, which fits in a bearing attached to the base plate. On the outside of the rotor cup a ring with sprocket teeth is glued.

<table>
<thead>
<tr>
<th>Table 7.2: Cost of rotor parts</th>
</tr>
</thead>
<tbody>
<tr>
<td>Iron cup</td>
</tr>
<tr>
<td>Magnets</td>
</tr>
<tr>
<td>Ring with sprocket teeth</td>
</tr>
<tr>
<td>Total:</td>
</tr>
</tbody>
</table>

7.1.3 Additional parts

All motor parts where discussed, but additional parts are required to assemble the motor and attach it to the feeder base plate. First a bearing is required, which sits on the base plate at the center of the stator part. An axle is glued to the rotor part, and put in the bearing, so that the rotor can rotate around this axis. Another important factor in the costs of the motor solution is the sensor system.

<table>
<thead>
<tr>
<th>Table 7.3: Cost additional parts</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axle</td>
</tr>
<tr>
<td>Bearing</td>
</tr>
<tr>
<td>Sensor system</td>
</tr>
<tr>
<td>Total:</td>
</tr>
</tbody>
</table>
7.1.4 Total cost

When all these estimated costs are added together, total costs will be between €14.75 and €19.50. Thus it is expected that such a motor can be created for €20. These prices are based on the dimensions of the previous derived design, with a diameter of around 95mm.

7.2 Extensive analysis

To have a more accurate estimate of the total costs of the motor and the main cost drivers, a cost price calculation was carried out by Ad Kieboom, external advisor product development. Again the initial design was used as a starting point, to get an estimate. A costbreakdown of this design is given in Table 7.5. As can be seen, the cost price is several times over the price target. One can identify 3 major cost drivers. The first is the rotor iron cup, which is very expensive since the teeth to transport the tape are directly milled in to it. The second is the coils. The 24 coils are seperately winded and glued, and later on placed on the teeth, after which the tooth tips are placed. The coils are also conical shaped, which make them complicated to wind. With this method, one can use the maximum slot area. However it is very expensive. Also a great portion of the assembly time is due to this placing of the coils and tooth tips. The third is the stator part made of solid steel, which is milled in to shape. This also includes the seperate tooth tips.

Table 7.4: Total costs

<table>
<thead>
<tr>
<th>Component</th>
<th>Cost (€)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator part</td>
<td>13.00</td>
</tr>
<tr>
<td>Rotor part</td>
<td>3.50</td>
</tr>
<tr>
<td>Additional parts</td>
<td>3.00</td>
</tr>
<tr>
<td><strong>Total:</strong></td>
<td><strong>19.50</strong></td>
</tr>
</tbody>
</table>

Table 7.5: Cost breakdown initial design (concept 1)

<table>
<thead>
<tr>
<th>Components:</th>
<th>Cost (€)</th>
</tr>
</thead>
<tbody>
<tr>
<td>20 pcs NdFeB-35 (9.5 x 7 x 2)</td>
<td>2</td>
</tr>
<tr>
<td>Iron ring</td>
<td>2</td>
</tr>
<tr>
<td>24 coils, 190 turns, 0.315 wire</td>
<td>27</td>
</tr>
<tr>
<td>Solid stator core (steel1010)</td>
<td>30</td>
</tr>
<tr>
<td>Ballbearing</td>
<td>2</td>
</tr>
<tr>
<td>Fixation of the stator</td>
<td>5</td>
</tr>
<tr>
<td>Position measuring</td>
<td>5</td>
</tr>
<tr>
<td>Printed Circuit Board</td>
<td>5</td>
</tr>
<tr>
<td><strong>Total:</strong></td>
<td><strong>77</strong></td>
</tr>
<tr>
<td>Logistics</td>
<td>7.70</td>
</tr>
<tr>
<td>Assembly &amp; overhead (70min):</td>
<td>34.26</td>
</tr>
<tr>
<td>Tooling, factory development &amp; profit</td>
<td>29.74</td>
</tr>
<tr>
<td><strong>Commercial price:</strong></td>
<td><strong>148.70</strong></td>
</tr>
</tbody>
</table>

In order to reduce costs, one should find alternatives for this three points. The costs of the rotor iron part can significantly be reduced when it is made from a simple steel pipe. Now
it no longer has the teeth on the outside. This can then be added by putting a die-casted plastic part on it. It would even be possible to integrate this with the rotating bearing part. Instead of winding all the conical coils separately, the coils can have a rectangular shape and be wound around the stator with a needle winder. In order to compensate for the loss in the amount of turns which are possible, one could apply stronger magnets and/or use cobalt-iron as the core material. Cobalt-iron has a saturation level of 2.2T, thus iron parts can be significantly smaller. The expensive stator part can be replaced by a stator created of transformer laminations, which is significantly cheaper. An overview of the cost when these measures are taken, is given in Table 7.6.

<table>
<thead>
<tr>
<th>Components:</th>
<th>Cost breakdown initial design</th>
</tr>
</thead>
<tbody>
<tr>
<td>20 pcs NdFeB-35 (9.5 x 7 x 2)</td>
<td>€2</td>
</tr>
<tr>
<td>Iron ring</td>
<td>€2</td>
</tr>
<tr>
<td>24 coils, 0.315 wire (needlewound)</td>
<td>€8</td>
</tr>
<tr>
<td>Laminated stator (transformer laminations)</td>
<td>€2</td>
</tr>
<tr>
<td>Ballbearing</td>
<td>€2</td>
</tr>
<tr>
<td>Fixation of the stator</td>
<td>€5</td>
</tr>
<tr>
<td>Position measuring</td>
<td>€5</td>
</tr>
<tr>
<td>Printed Circuit Board</td>
<td>€3</td>
</tr>
<tr>
<td><strong>Total:</strong></td>
<td><strong>€29</strong></td>
</tr>
<tr>
<td>Logistics</td>
<td>€2.90</td>
</tr>
<tr>
<td>Assembly &amp; overhead (8min):</td>
<td>€3.92</td>
</tr>
<tr>
<td>Tooling, factory development &amp; profit</td>
<td>€12.08</td>
</tr>
<tr>
<td><strong>Commercial price:</strong></td>
<td><strong>€47.90</strong></td>
</tr>
</tbody>
</table>

Further cost reduction can be obtained by replacing the ball bearing with a Delrin bearing. Also the fixation of the stator can be made in delrin. The outline of costs is given in Table 7.7.

The decision to change the shape of the coils, reduces the amount of slot area that can be used to place the windings. Therefore it is interesting to know what can be done to compensate for this effect, and how this affects the cost of the motor. There are two simple ways to do this: placing stronger magnets, and replacing the iron with for example cobalt-iron (as mentioned before). Whether these things can really compensate the effect will be made clear in the next chapter, were the performance impact of the design choices will be investigated.

As can be seen in Table 7.8, the stronger magnets are much more expensive than the earlier chosen GSN-35 variant. But impact of the magnet costs on the total price is very low, thus GSN-50 magnets are a serious option to investigate. This story is different for the core material. Replacing the iron with cobalt-iron makes the stator very expensive. In the next chapter the performance for this option is also calculated, but due to the high costs it can hardly be considered to be a serious option for our application.
### Table 7.7: Cost breakdown (concept 3)

<table>
<thead>
<tr>
<th>Components:</th>
<th>Cost breakdown final concept</th>
</tr>
</thead>
<tbody>
<tr>
<td>20 pcs NdFeB-35 (9.5 x 7 x 2)</td>
<td>€2</td>
</tr>
<tr>
<td>Iron ring</td>
<td>€2</td>
</tr>
<tr>
<td>24 coils, 0.315 wire (needlewound)</td>
<td>€8</td>
</tr>
<tr>
<td>Laminated stator (transformer laminations)</td>
<td>€2</td>
</tr>
<tr>
<td>Delrin bearing</td>
<td>€0.20</td>
</tr>
<tr>
<td>Fixation of the stator (Delrin)</td>
<td>€0.20</td>
</tr>
<tr>
<td>Position measuring</td>
<td>€5</td>
</tr>
<tr>
<td>Printed Circuit Board</td>
<td>€2</td>
</tr>
<tr>
<td><strong>Total:</strong></td>
<td><strong>€21.40</strong></td>
</tr>
<tr>
<td><strong>Logistics</strong></td>
<td><strong>€2.14</strong></td>
</tr>
<tr>
<td><strong>Assembly &amp; overhead (8min):</strong></td>
<td><strong>€2.94</strong></td>
</tr>
<tr>
<td><strong>Tooling, factory development &amp; profit</strong></td>
<td><strong>€8.38</strong></td>
</tr>
<tr>
<td><strong>Commercial price:</strong></td>
<td><strong>€34.86</strong></td>
</tr>
</tbody>
</table>

### Table 7.8: Alternative materials

<table>
<thead>
<tr>
<th>Material</th>
<th>Cost</th>
</tr>
</thead>
<tbody>
<tr>
<td>NdFeB-35 / GSN-35</td>
<td>€2</td>
</tr>
<tr>
<td>NdFeB-50 / GSN-50</td>
<td>€2.82</td>
</tr>
<tr>
<td>Stator of transformer laminations</td>
<td>€2</td>
</tr>
<tr>
<td>Stator of Cobalt-Iron (Vacoflux-50) laminations</td>
<td>€25</td>
</tr>
</tbody>
</table>
Chapter 8

Performance Calculations

In the previous chapter some possibilities were given to reduce the costs of the motor. It is important to know what the impact of these changes are on the performance. Also some other design variables are looked at: various numbers of slots / poles are examined, and the impact of power supply limits are investigated. Each time one variable is changed, the initial sizing rules are used to generate a new initial design. This results in a design and an initial estimate of its torque capability. This design is then analysed by the MEC models, to have a more founded estimation of torque. With this results the candidates showing good performance can be found, and can be examined more closely by means of FEM.

8.1 Diameter variation

The first parameter which is analysed is the motor diameter. The rotor outer diameter ($D_{ro}$) is changed from 90 to 50mm. Each time the initial sizing rules are used to size the other parts: airgap diameter, width of teeth, height of the back-iron, height of slots and number of turns. This results in Table 8.1, showing the realized torque and corresponding pulling force at the rated tape speed.\(^1\)

<table>
<thead>
<tr>
<th>$D_{gap}$</th>
<th>$D_{ro}$</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>99.6 mm</td>
<td>0.63 Nm</td>
<td>0.70 Nm</td>
<td>0.60 Nm</td>
<td>0.62 Nm</td>
<td>12.5 N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>91.7 mm</td>
<td>0.54 Nm</td>
<td>0.59 Nm</td>
<td>0.51 Nm</td>
<td>0.51 Nm</td>
<td>11.2 N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>72.8 mm</td>
<td>0.32 Nm</td>
<td>0.34 Nm</td>
<td>0.30 Nm</td>
<td>0.30 Nm</td>
<td>8.6 N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>53.9 mm</td>
<td>0.13 Nm</td>
<td>0.11 Nm</td>
<td>0.10 Nm</td>
<td>0.13 Nm</td>
<td>5.1 N</td>
</tr>
</tbody>
</table>

As expected, the realised torque rises with the motor diameter. Also notice the fact that none of the motors listed in the table is able to produce the 15N pull force at the rated speed. It can also be seen that the gain from using the optimization routine is very small, while computation times are very large. This means that the initial sizing rules were good,

---

\(^1\)The numbers for the airgap diameter seem to be choosen in a strange way. That is because they were choosen to have an outer motor diameter of 100mm, 90mm, 70mm and 50mm respectively. However in the process of removing all errors from the sizing equations some small deviations in outer diameters were introduced. But this is of no further importance.
and that all iron parts were sized near the point with maximum torque. Therefore, this optimization routine was skipped in the following tables.

8.2 Conical vs. rectangular coils

From the cost perspective the conical coils (filling the entire slot) are very expensive. Therefore it is interesting to look at coils with a rectangular shape, thus having the same width from the bottom of the slot to the top. This requires some new equations for choosing the slot height and calculating the single turn length.

The effect of the teeth height on the useable coil area for straight coils is illustrated in Figure 8.1. The height of the teeth can be found by equation 8.3.

\[
A_{\text{coil}} = \frac{1}{2} w_{\text{bottom}} h_t \\

w_{\text{bottom}} = \frac{\pi (D_{so} - 2h_t - 2h_{\text{shoe}}) - N_s w_t}{N_s} \tag{8.2}
\]

\[
h_t = \arg\max_{h_t} A_{\text{coil}} \tag{8.3}
\]

\[
l_t = 2l + 2w_t + \pi (w_{\text{bottom}}) \tag{8.4}
\]

When the conical coils are replaced with rectangular ones while keeping all the rest the same, a decrease in torque of around 10% is predicted. Off course because of the different length of a turn and thus other winding resistance, the amount of turns is recalculated to maximize electrical loading again.

<table>
<thead>
<tr>
<th>(D_{\text{gap}})</th>
<th>(D_{ro})</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>99.6 mm</td>
<td>0.56Nm</td>
<td>0.63Nm</td>
<td>0.53Nm</td>
<td>Nm</td>
<td>10.8N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>91.7 mm</td>
<td>0.48Nm</td>
<td>0.53Nm</td>
<td>0.45Nm</td>
<td>Nm</td>
<td>10.1N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>72.8 mm</td>
<td>0.31Nm</td>
<td>0.32Nm</td>
<td>0.28Nm</td>
<td>Nm</td>
<td>7.9N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>53.9 mm</td>
<td>0.17Nm</td>
<td>0.15Nm</td>
<td>0.13Nm</td>
<td>Nm</td>
<td>5.1N</td>
</tr>
</tbody>
</table>

Another effect is that the rectangular coils have a much shorter end turn length. This gives the possibility to increase the active length of the motor, compensating for this loss. An increase in torque compared to the conical shaped coils might even be possible.
8.2. CONICAL VS. RECTANGULAR COILS

Figure 8.2: Conical (left) vs. rectangular coils (right)

Table 8.3: Conical vs rectangular coils

<table>
<thead>
<tr>
<th>Active length</th>
<th>Conical</th>
<th>Rectangular</th>
<th>Rectangular</th>
</tr>
</thead>
<tbody>
<tr>
<td>7mm</td>
<td>7mm</td>
<td>7mm</td>
<td>10mm</td>
</tr>
<tr>
<td>13.3mm</td>
<td>10.2mm</td>
<td>13.2mm</td>
<td></td>
</tr>
<tr>
<td>Torque</td>
<td>0.60Nm</td>
<td>0.53Nm</td>
<td>0.68Nm</td>
</tr>
</tbody>
</table>

As can be seen in Table 8.3 the use of the conical shaped coils was not a good option. While it maximizes the used slot area, the amount of lost space due to the end windings is too large. It is interesting to see the numbers for the motors with straight coils and an active length of 10mm, since these have roughly the same volume as previous motors.

Table 8.4: Rectangular coils, with 10mm active length

<table>
<thead>
<tr>
<th>D_{gap}</th>
<th>D_{ro}</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>99.6 mm</td>
<td>0.72Nm</td>
<td>0.78Nm</td>
<td>0.68Nm</td>
<td>Nm</td>
<td>13.8N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>91.7 mm</td>
<td>0.62Nm</td>
<td>0.66Nm</td>
<td>0.58Nm</td>
<td>Nm</td>
<td>12.8N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>72.8 mm</td>
<td>0.40Nm</td>
<td>0.41Nm</td>
<td>0.35Nm</td>
<td>Nm</td>
<td>10.1N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>53.9 mm</td>
<td>0.21Nm</td>
<td>0.19Nm</td>
<td>0.16Nm</td>
<td>Nm</td>
<td>6.4N</td>
</tr>
</tbody>
</table>

Note that in this analysis, the additional space required for the needle winder is not reserved yet. This will again result in a reduction of the slot area, and a decrease in torque capability. Designs having a high slot count will be affected the most. If for example 2mm of needle space is reserved the torque for the 86.6mm airgap diameter design is decreased from 0.72Nm to 0.55Nm, which is almost a 25% reduction. All the calculations presented in this chapter were done without taking this effect into account, since these were already done before it was implemented.
8.3 Magnet material

A different aspect which greatly influences performance of the motor is the magnet material. Stronger magnets are generally more expensive, therefore a consideration is required. Strong magnets result in higher flux densities in the air gap (magnet loading), resulting in a higher torque and backEMF constant. This gives a motor which generates more torque but has a lower maximum speed. Another aspect of choosing the right magnet material is the material grading. A higher grading is a material with a higher quality and purity, resulting in stronger magnets. For the application here, only Neodymium magnets are a candidate, for their high strength and relatively low costs (when compared to Samarium Cobalt magnets). Magnet data for several gradings of NdFeB is given in Table 8.5.

Table 8.5: Magnet materials

<table>
<thead>
<tr>
<th></th>
<th>Br [T]</th>
<th>Hcb [kA/m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>GSN-35 / BM-35</td>
<td>1.22</td>
<td>891</td>
</tr>
<tr>
<td>GSN-40 / BM-40</td>
<td>1.30</td>
<td>891</td>
</tr>
<tr>
<td>GSN-50 / BM-40</td>
<td>1.43</td>
<td>859</td>
</tr>
</tbody>
</table>

Now, the same calculations are done with these stronger magnets. Again, the initial sizing rules are used to come to a new design, since more iron is required to conduct the magnetic flux. Remember that most sizing equations use the airgap diameter as a starting point. Therefore this is kept the same as in earlier calculations. As a result, the outer diameter of the motors is slightly increased, since the back-iron has to be slightly larger to carry the extra flux.

Table 8.6: GSN-40 magnet material

<table>
<thead>
<tr>
<th>D_{gap}</th>
<th>D_{ro}</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>99.9 mm</td>
<td>0.72 Nm</td>
<td>0.78 Nm</td>
<td>0.69 Nm</td>
<td>Nm</td>
<td>14.0 N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>92.1 mm</td>
<td>0.62 Nm</td>
<td>0.67 Nm</td>
<td>0.59 Nm</td>
<td>Nm</td>
<td>13.0 N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>73.0 mm</td>
<td>0.40 Nm</td>
<td>0.41 Nm</td>
<td>0.36 Nm</td>
<td>Nm</td>
<td>10.0 N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>54.1 mm</td>
<td>0.22 Nm</td>
<td>0.19 Nm</td>
<td>0.16 Nm</td>
<td>Nm</td>
<td>6.5 N</td>
</tr>
</tbody>
</table>

Table 8.7: GSN-50 magnet material

<table>
<thead>
<tr>
<th>D_{gap}</th>
<th>D_{ro}</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>100.5 mm</td>
<td>0.73 Nm</td>
<td>0.78 Nm</td>
<td>0.70 Nm</td>
<td>Nm</td>
<td>14.2 N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>92.6 mm</td>
<td>0.63 Nm</td>
<td>0.67 Nm</td>
<td>0.60 Nm</td>
<td>Nm</td>
<td>13.3 N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>73.4 mm</td>
<td>0.41 Nm</td>
<td>0.41 Nm</td>
<td>0.37 Nm</td>
<td>Nm</td>
<td>10.5 N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>54.4 mm</td>
<td>0.22 Nm</td>
<td>0.19 Nm</td>
<td>0.17 Nm</td>
<td>Nm</td>
<td>6.7 N</td>
</tr>
</tbody>
</table>

As can be seen, the gain in output torque is only marginal. This comes from the fact that with the increased amount of flux from the magnets, the teeth need to be increased in size as well, resulting in a lower electrical loading. In the case of steel1010 iron parts, the flux density is limited to approximately 1.7T, and the increase in magnetic loading and the
resulting decrease in electrical loading cancel each other. When a material is used with a higher saturation level, the benefit from the increase in magnetic loading might be larger than the loss from the lower electrical loading, and in that case stronger magnets might be beneficial.

8.4 Core material

Having a material with a higher saturation allows reduction of the iron sizes, resulting in more space for windings. Therefore it is interesting to see what torques can be expected when cobalt-iron (Vacoflux-50) is used as core material. In the cost analysis it was seen that this material is expensive, but if it increases torque, motor diameter can be decreased.

![Figure 8.3: BH curve for steel1010 and Vacoflux-50](image)

<table>
<thead>
<tr>
<th>$D_{gap}$</th>
<th>$D_{ro}$</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>97.8 mm</td>
<td>0.82 Nm</td>
<td>0.87 Nm</td>
<td>0.81 Nm</td>
<td>Nm</td>
<td>16.5 N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>90.1 mm</td>
<td>0.70 Nm</td>
<td>0.74 Nm</td>
<td>0.69 Nm</td>
<td>Nm</td>
<td>15.3 N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>71.4 mm</td>
<td>0.45 Nm</td>
<td>0.46 Nm</td>
<td>0.43 Nm</td>
<td>Nm</td>
<td>12.2 N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>52.9 mm</td>
<td>0.26 Nm</td>
<td>0.22 Nm</td>
<td>0.20 Nm</td>
<td>Nm</td>
<td>8.1 N</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>$D_{gap}$</th>
<th>$D_{ro}$</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6 mm</td>
<td>98.9 mm</td>
<td>0.87 Nm</td>
<td>0.90 Nm</td>
<td>0.86 Nm</td>
<td>Nm</td>
<td>17.5 N</td>
</tr>
<tr>
<td>79.3 mm</td>
<td>91.1 mm</td>
<td>0.74 Nm</td>
<td>0.77 Nm</td>
<td>0.74 Nm</td>
<td>Nm</td>
<td>16.4 N</td>
</tr>
<tr>
<td>61.6 mm</td>
<td>72.3 mm</td>
<td>0.48 Nm</td>
<td>0.48 Nm</td>
<td>0.46 Nm</td>
<td>Nm</td>
<td>13.0 N</td>
</tr>
<tr>
<td>44.0 mm</td>
<td>53.6 mm</td>
<td>0.26 Nm</td>
<td>0.23 Nm</td>
<td>0.22 Nm</td>
<td>Nm</td>
<td>8.6 N</td>
</tr>
</tbody>
</table>
8.5 Power supply

Another limit on the produced torque is the power supply which is available. To illustrate this impact, torques are recalculated for other current and voltage limits. When the current limit is increased to 1.0A, the results of Table 8.10 are obtained. However, when doing this, the current density in the wires will rise significantly, from $5A/mm^2$ to around $14A/mm^2$. With the slot fill factor of 0.7, the slot current density rises from $3.5A/mm^2$ to $9.8A/mm^2$. This will most likely result in thermal problems. Generally a maximum slot current density of $5A/mm^2$ is used for totally enclosed machines [5]. Note that this is rating is for continuous use, while feeders are usually operated at low duty cycles, which could allow peak currents several times higher. In that case diameter of the motor could be significantly reduced. One should do thermal analysis to investigate this effects.

As expected, the produced torque is significantly larger. A motor with a diameter of 70mm is almost able to produce sufficient torque to drive the tape.

8.6 Sinusoidal vs. square excitation

Thus far, all calculations were done for machines with square (six step / trapezoidal) commutation. To see the difference with sinusoidal commutation some calculations were done with sinusoidal currents. To get a good comparison, the RMS value of the phase currents ($I_{\text{rms}}$) is kept the same.

$$I_{\text{rms}} = \frac{2}{3}I_{\text{max}} = \frac{2}{3}0.5$$  \hspace{1cm} (8.5)

$$I_{\text{sin}} = I_{\text{top}}\sin\theta_e$$  \hspace{1cm} (8.6)

$$I_{\text{top}} = \sqrt{2}I_{\text{rms}} = \sqrt{2}\left(\frac{2}{3}\right)0.5$$  \hspace{1cm} (8.7)

As can be seen in Figure 8.4, the produced torque is significantly larger when square excitation is used. The sinusoidal excitation however, results in a smaller torque ripple. But since in this application it was important to produce as many torque from the volume as possible, square excitation was the correct decision.

8.7 Number of poles and slots

Changing the number of poles and slots while keeping active length the same, does have some influence on the realised torque. This can be seen in Table 8.12. Note that for each different
number of slots and poles another winding lay-out needs to be determined. An overview of the winding lay-outs for the used combinations is given in Table 8.11.

However, when again the effect on the end turn length is taken into account, motors with a high number of slots can have a higher active length.
### Table 8.12: Various numbers of poles and slots

<table>
<thead>
<tr>
<th>slots / poles</th>
<th>active length</th>
<th>total length</th>
<th>Torque</th>
<th>Force</th>
</tr>
</thead>
<tbody>
<tr>
<td>12 / 10</td>
<td>7 mm</td>
<td>13.5 mm</td>
<td>0.47 Nm</td>
<td>8.9 N</td>
</tr>
<tr>
<td>24 / 20</td>
<td>7 mm</td>
<td>10.2 mm</td>
<td>0.53 Nm</td>
<td>10.8 N</td>
</tr>
<tr>
<td>24 / 22</td>
<td>7 mm</td>
<td>10.2 mm</td>
<td>0.57 Nm</td>
<td>11.5 N</td>
</tr>
<tr>
<td>24 / 26</td>
<td>7 mm</td>
<td>10.2 mm</td>
<td>0.55 Nm</td>
<td>11.3 N</td>
</tr>
<tr>
<td>24 / 28</td>
<td>7 mm</td>
<td>10.2 mm</td>
<td>0.58 Nm</td>
<td>11.9 N</td>
</tr>
<tr>
<td>27 / 24</td>
<td>7 mm</td>
<td>9.9 mm</td>
<td>0.54 Nm</td>
<td>11.0 N</td>
</tr>
<tr>
<td>36 / 30</td>
<td>7 mm</td>
<td>9.2 mm</td>
<td>0.54 Nm</td>
<td>11.1 N</td>
</tr>
</tbody>
</table>

### Table 8.13: Various numbers of poles and slots (fixed length)

<table>
<thead>
<tr>
<th>slots / poles</th>
<th>active length</th>
<th>total length</th>
<th>Torque</th>
<th>Force</th>
</tr>
</thead>
<tbody>
<tr>
<td>12 / 10</td>
<td>7 mm</td>
<td>13.5 mm</td>
<td>0.47 Nm</td>
<td>8.9 N</td>
</tr>
<tr>
<td>24 / 20</td>
<td>10 mm</td>
<td>13.2 mm</td>
<td>0.68 Nm</td>
<td>13.7 N</td>
</tr>
<tr>
<td>24 / 22</td>
<td>10 mm</td>
<td>13.2 mm</td>
<td>0.72 Nm</td>
<td>14.5 N</td>
</tr>
<tr>
<td>24 / 26</td>
<td>10 mm</td>
<td>13.2 mm</td>
<td>0.68 Nm</td>
<td>13.7 N</td>
</tr>
<tr>
<td>24 / 28</td>
<td>10 mm</td>
<td>13.2 mm</td>
<td>0.73 Nm</td>
<td>15.0 N</td>
</tr>
<tr>
<td>27 / 24</td>
<td>10.3 mm</td>
<td>13.2 mm</td>
<td>0.70 Nm</td>
<td>14.1 N</td>
</tr>
<tr>
<td>36 / 30</td>
<td>11 mm</td>
<td>13.2 mm</td>
<td>0.72 Nm</td>
<td>14.9 N</td>
</tr>
</tbody>
</table>
8.8 Combining results

The best option from a cost perspective to fulfill the torque requirements seems to be a motor incorporating NdFeB-50 material, having rectangular coils and made of laminated iron. Using this combination, with 20 poles and 24 slots yield the following results:

<table>
<thead>
<tr>
<th>$D_{gap}$ (mm)</th>
<th>$D_{ro}$ (mm)</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force (N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6</td>
<td>100.5</td>
<td>0.73 Nm</td>
<td>0.78 Nm</td>
<td>0.70 Nm</td>
<td>Nm</td>
<td>14.2</td>
</tr>
<tr>
<td>79.3</td>
<td>92.6</td>
<td>0.63 Nm</td>
<td>0.67 Nm</td>
<td>0.60 Nm</td>
<td>Nm</td>
<td>13.3</td>
</tr>
<tr>
<td>61.6</td>
<td>73.4</td>
<td>0.41 Nm</td>
<td>0.41 Nm</td>
<td>0.37 Nm</td>
<td>Nm</td>
<td>10.5</td>
</tr>
<tr>
<td>44.0</td>
<td>54.4</td>
<td>0.22 Nm</td>
<td>0.19 Nm</td>
<td>0.17 Nm</td>
<td>Nm</td>
<td>6.7</td>
</tr>
</tbody>
</table>

The option yielding maximum torque is a motor which uses cobalt-iron as its core material, NdFeB-50 magnets, rectangular coils and an active length of 10mm.

<table>
<thead>
<tr>
<th>$D_{gap}$ (mm)</th>
<th>$D_{ro}$ (mm)</th>
<th>Initial estimate</th>
<th>MEC linear</th>
<th>MEC non-linear</th>
<th>MEC opt.</th>
<th>Pull force (N)</th>
</tr>
</thead>
<tbody>
<tr>
<td>86.6</td>
<td>98.9</td>
<td>0.87 Nm</td>
<td>0.90 Nm</td>
<td>0.86 Nm</td>
<td>Nm</td>
<td>17.5</td>
</tr>
<tr>
<td>79.3</td>
<td>91.1</td>
<td>0.74 Nm</td>
<td>0.77 Nm</td>
<td>0.74 Nm</td>
<td>Nm</td>
<td>16.3</td>
</tr>
<tr>
<td>61.6</td>
<td>72.3</td>
<td>0.48 Nm</td>
<td>0.48 Nm</td>
<td>0.46 Nm</td>
<td>Nm</td>
<td>13.0</td>
</tr>
<tr>
<td>44.0</td>
<td>53.6</td>
<td>0.26 Nm</td>
<td>0.23 Nm</td>
<td>0.22 Nm</td>
<td>Nm</td>
<td>8.6</td>
</tr>
</tbody>
</table>

8.9 Proposed design

From the conclusions in the previous section it is possible to make a cost effective motor design. As stated there, this should be a motor with a laminated core of transformer laminations, having 1.4T magnets (GSN-50), and straight coils. Further the total height should be limited to around 13mm. If this combination is used in the iterative sizing equations, the following design is obtained:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_{ro}$</td>
<td>110mm</td>
</tr>
<tr>
<td>$D_{gap}$</td>
<td>96.1mm</td>
</tr>
<tr>
<td>$h_m$</td>
<td>3.2mm</td>
</tr>
<tr>
<td>$h_{bi}$</td>
<td>3.9mm</td>
</tr>
<tr>
<td>$w_l$</td>
<td>5.9mm</td>
</tr>
<tr>
<td>$h_l$</td>
<td>12.0mm</td>
</tr>
<tr>
<td>$l$</td>
<td>10.0mm</td>
</tr>
<tr>
<td>$N$</td>
<td>135</td>
</tr>
<tr>
<td>wire diameter</td>
<td>0.36mm</td>
</tr>
<tr>
<td>total length</td>
<td>13.2mm</td>
</tr>
</tbody>
</table>
Figure 8.5: Drawings of the proposed design
8.10 Reserving space for needle winder

As mentioned in the cost analysis, having the coils wound by a needle winder, drastically reduces the costs of the motor. In the previous design the additional space which should be reserved for the needle to move through was not taken in to account. The impact of this on the torque production is shown in Table 8.17. Here the diameter is left unchanged, 2mm needlespace is reserved, and the maximum torque is calculated. When the sizing equations are altered to include this extra area, required motor diameter is increased significantly (Table 8.18). If for example 2mm is reserved between two adjacent coils, the outer diameter rises from 110 to 154mm. This is illustrated in Figure 8.6. With 1mm space this is 133mm.

8.11 Alternative design

To further reduce the cost of the motor, it could be possible to replace the rotor with the 20 separate magnets by 2 toothed shaped iron rings with a single permanent magnet in between,
Table 8.18: Impact of needle space on required diameter

<table>
<thead>
<tr>
<th></th>
<th>No space reserved</th>
<th>Needle space reserved</th>
</tr>
</thead>
<tbody>
<tr>
<td>required diameter</td>
<td>Pull Force</td>
<td>required diameter</td>
</tr>
<tr>
<td>24 slots</td>
<td>110mm</td>
<td>14.5N</td>
</tr>
</tbody>
</table>

A drawback of this solution is the reduced effective airgap area, since the teeth cover only about half the airgap length. However where the separate poles only covered 70 percent of the circumference (the pole embrace), it can be significantly larger here. To see the effect on the performance, the same stator was taken as in the proposed solution, but now with the new rotor. These FEM calculations showed an decrease in torque roughly equal to the reduced effective length. Another option is to create multiple slots on a single teeth, the same as done in a hybrid stepper motor (this is illustrated in Fig. 8.8). This gives the ability to choose the number of poles and slots, more or less independent of the number of coils. Increasing the number of slots on a single teeth, also increases the electrical frequency of the drive, which can be beneficial. Drawback of using this kind of rotors is the fact that the stator core can no longer be made of laminated iron, since flux has to travel in axial direction as well. Thus it is not sure whether it would really decrease the cost of the motor, since another technique for the stator has to be used.
Figure 8.8: Axial field motor
Chapter 9

Future feeders

9.1 Trends in feeder design

It is difficult to predict what the trend in feeder design will be. Assembléon is currently doing a lot of research to determine in which direction their development should go. This graduation project is also part of this research.

9.2 Feeder dimensions

It is most likely that in the near future, only single lane feeders will be used. This reduces the maximum number of different components a single machine can handle during production. Therefore it is expected that in the future the feeder pitch (the space for a feeder on the machine) will be reduced. Having a 16mm pitch while the component tape is 8mm wide, means that a lot of machine space is lost. With the ongoing miniaturization of components on PCBs the tape width is even further reduced to 4mm for the small components. Therefore, it is interesting to estimate the torque capabilities for motors with a further reduced axial length.

Using the sizing routines, a table is generated showing the tape pull force for various dimensions of the motor.

<table>
<thead>
<tr>
<th>Rated speed</th>
<th>$D_{ro}$ 54.4 mm</th>
<th>73.4 mm</th>
<th>92.6 mm</th>
<th>100.5 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>28.1 rpm</td>
<td>20.8 rpm</td>
<td>16.5 rpm</td>
<td>15.2 rpm</td>
</tr>
<tr>
<td>14 mm</td>
<td>0.25 Nm / 9.2 N</td>
<td>0.44 Nm / 12.1 N</td>
<td><strong>0.69 Nm / 15.0 N</strong></td>
<td>0.80 Nm / 15.9 N</td>
</tr>
<tr>
<td>13 mm</td>
<td>0.24 Nm / 8.8 N</td>
<td>0.44 Nm / 11.9 N</td>
<td>0.65 Nm / 14.1 N</td>
<td><strong>0.75 Nm / 15.0 N</strong></td>
</tr>
<tr>
<td>10 mm</td>
<td>0.20 Nm / 7.3 N</td>
<td>0.36 Nm / 9.8 N</td>
<td>0.52 Nm / 11.2 N</td>
<td>0.59 Nm / 11.8 N</td>
</tr>
<tr>
<td>8 mm</td>
<td>0.17 Nm / 6.1 N</td>
<td>0.29 Nm / 8.0 N</td>
<td>0.41 Nm / 8.9 N</td>
<td>0.46 Nm / 9.2 N</td>
</tr>
</tbody>
</table>

It is obvious that when smaller motors are required, the achievable pull force decreases significantly. Gearing will become necessary when the required pull force of 15N still needs to me met. But when gears are used, it might be better to dimension motors to have a high efficiency instead of having the maximum torque at a rated speed. The motors should now maximize torque times speed, which is power. The numbers in the table above are for...
motors which were optimized to deliver maximum torque at a rotor speed of 0.08m/s at the circumference. When gearing is used, the speed at which the motor runs increases, thus the numbers for the force can not be used directly.

9.3 Indexing time

For pick and place machines, the total output (components per hour) is crucial. In the current solution the indexing time of the feeders is not the limiting factor, since after each pick the robot arm moves to the board, places the component and comes back. This travel time is determining the total output of the machine. This is also the time available for the feeder to ensure that the next component is ready for the following pick. In the ongoing demand for more machine output, travel times are going down. At some point, the index time of the feeder can become a limiting factor. Therefore the index time stated in the requirements was already half of the index time of current feeders. Another possibility is to develop machines which use the so called "collect and place" strategy. In these machines, the head can carry several components. It picks until all component positions are full, then travels to the board and places them sequentially. In such a machine, the index time of the feeders is of much greater importance, since the head could pick multiple components from a single feeder, before it travels to the board. This strategy is already used in the Assembléon AX-201 machine. When decreasing the indexing time of the feeder, the acceleration levels of the tape need to go up. This means that the inertia of the reel, but also of the rotor of the motor become more important. With the 100ms index time used in the research done here, these are still neglectable compared to the friction force. However, this should be kept in mind when indexing times need to be decreased further.
Chapter 10

Conclusions

In the previous chapters, a model was derived for estimating the performance of a direct-drive torque motor for use in tape feeders. Several design choices have been investigated, together with their impact on the motor performance and costs. This should be a good tool to decide whether or not to do further research on the direct drive solution for tape feeders.

To come to a motor which develops sufficient torque to index the tape, rather high diameter motors are required, filling the entire front area of the feeder. It was shown to be infeasible within the requirements of chapter 2. However if the friction force can be reduced from 15N to for example 13N, a motor with a diameter slightly over 90mm will be sufficient (see Table 8.14), and will fit within the maximum dimensions.

The same goes for the power budget. If it is not possible to reduce the friction force, a small increase in the power budget might be sufficient to be able to design a motor within the other specifications. An example of this was given in Table 8.10, in which the current limit was doubled to 1A, and a motor with diameter of 73mm would develop sufficient torque (note that here no space is reserved for needle winding). But before increasing the current limit one should do a thermal analysis, since the motors already have a relatively low efficiency when operated in the point of maximum torque.

When it comes to the sensing part, it was shown that this would be difficult for direct-drive solutions. The very high required resolution is difficult to reach with standard (low cost) solutions. Although there are some development in this area, which might make these resolutions available a few years from now. Also the cost aspect was investigated, and the analysis has shown that a typical motor would cost around €35. This is significant over the price target of €20. It should be considered whether the benefits of direct-drive feeding can justify the price.

Looking at these facts, it can be concluded that even when there will be sufficient space available in future feeders, there are still a lot of difficulties for using a direct-drive solution in the tape feeders.

Summarizing:

- A Direct Drive solution is possible, but requires very large diameters up to 150mm (with 2 mm needle winding space reserved), if motor length is limited to 13mm.

- A decrease in motor diameter can be realized (110mm) when the current limit is increased (1A), but thermal analysis is required. Reduction of the 15N required force
results in much smaller motors. (10N requires 106mm instead of 154mm)

• When feeder width is decreased, direct drive is only possible, if the 15N is decreased significantly.

• It is difficult to find a good sensor system having sufficiently high resolution, but technology is almost at required level.

• Cost price of the proposed design is still much above price target.

• Combining the very high volume requirement for such a solution with the difficulties of sensoring and high cost price, it will be more beneficial to look at other concepts, for example a small disk type motor with straight gears.
Bibliography


